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**An Investigation of the Aerodynamic Effects
of a Two-Radome Installation on a Challenger
Aircraft**

by

Zubin Chinoy, B.Eng.

A thesis submitted to
the Faculty of Graduate Studies and Research
in partial fulfilment of
the requirements for the degree of
Masters of Engineering

Ottawa-Carleton Institute for
Mechanical and Aerospace Engineering

Department of
Mechanical and Aerospace Engineering
Carleton University
Ottawa, Ontario
April 27th, 1998

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acceptance of the thesis

**An Investigation of the Aerodynamic Effects of a
Two-Radome Installation on a Challenger Aircraft**

submitted by
Zubin Chinoy

in partial fulfilment of the requirements for
the degree of *Master of Engineering*

Chairman,
Department of Mechanical and Aerospace Engineering

Thesis Supervisor

Carleton University

May 25th, 1998

Abstract

This thesis documents an investigation into the aerodynamic effects of placing two large radomes in series on the underbelly of a Challenger aircraft. The investigation was funded by the Department of National Defence (DND) of Canada. DND plans to fit some of their Challenger aircraft with these radomes in order to house electronic equipment. The investigation was mainly experimental, although inviscid panel-method computations were used to help determine an optimal wind-tunnel model size and for exploring inviscid trends with aircraft speed and attitude changes. Experimental work was performed in Carleton University's low-speed closed circuit wind-tunnel. Reynolds number effects were minimized by tripping the approach-flow floor boundary layer to produce fully turbulent flow. Total drag, lift and pitching moment perturbations due to the radome installations on the aircraft were investigated for three radome separation distances: design, $1.1 \times$ design and $0.8 \times$ design. The wake survey method was used to measure total drag force and surface static pressure measurements gave lift, pressure-drag, and pitching moment. At $0.8 \times$ design radome separation distance, a bi-stable wake configuration was observed downstream of the aft-radome. It is thought that a slight asymmetric disturbance in the flow over the forward-radome was further amplified by the close proximity of the aft-radome. Within the uncertainties of the measurements, changes in the aircraft lift, drag and pitching moment due to the radomes were approximately the same for all three radome separation distances. Oil-flow visualization studies showed a three-dimensional separation line on each radome and a 2-vortex flow structure in the plane of symmetry upstream of each radome. This vortex structure consisted of one large 'primary' or 'horseshoe' vortex and one small tightly-wound body/floor

corner vortex of opposite sign. Both vortices trailed downstream on either side of each radome. Contour plots of momentum-flux-deficit, obtained from wake surveys downstream of the radomes, clearly reflected the effects of these horseshoe vortices. All aerodynamic coefficients were corrected for compressibility effects, which were approximately accounted for by using the first Prandtl-Glauert rule.

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List of Symbols

$A, \Delta A$	cross-section area: test-section; measurement plane/radome panel
$C_d, \Delta C_d$	total drag coefficient: generic, $C_d = \frac{D}{P_{dyn} S_{for}}$; incremental (Eqn. 3.10)
$C_l, \Delta C_l$	lift coefficient: generic, $C_l = \frac{L}{P_{dyn} S_{for}}$; incremental (Eqn. 3.13)
C_m	pitching moment coefficient (Eqn. 3.17)
C_p	pressure coefficient (Eqn. 3.12)
$C_{pd}, \Delta C_{pd}$	pressure-drag coefficient: generic; incremental (Eqn. 3.14)
D	drag force
δ	boundary-layer thickness
K	wind-tunnel contraction calibration constant
L	lift force
L, W, H	length, width and height of radome
M	Mach number
$\hat{n}$	panel unit-normal vector
P_{c_1}, P_{c_2}	reference pressures on wind-tunnel contraction section
P_{dyn}	dynamic pressure, $\frac{1}{2} \rho V_\infty^2$
P_∞	undisturbed freestream static pressure
P_t, P_s	total and static pressures
Re	Reynolds number based on forward-radome width
S_{for}	three-dimensional surface area of forward radome
V	pressure transducer voltage
V_∞	freestream velocity
w_R	uncertainty in a given quantity
x, y, z	test-section coordinates: axial; spanwise; vertical
x_{tr}	axial location of laminar to turbulent transition

Chapter 1

Introduction

An investigation of the aerodynamic effects of mounting two large radomes in series (i.e. one downstream of the other) on the underside of the fuselage of a Challenger aircraft is reported. Of particular interest is the increase in drag, loss in lift and change in pitching moment experienced by the aircraft due to the radome installations. Three radome separation distances were tested: design, $0.8 \times$ design, and $1.1 \times$ design. This study was supported by a contract from the Department of National Defence (DND) of Canada, which plans to equip some of their fleet of Challenger aircraft with electronic surveillance equipment, housed in these radomes.

A combination of computational and experimental work was carried out to evaluate the aerodynamic quantities of interest. The computational work involved using a panel method code called PMARC (Panel Method Ames Research Centre), developed by NASA and similar to the code VSAERO developed by Analytical Methods Inc. PMARC was used initially to evaluate the wind-tunnel test-section wall interference and model blockage effects in order to select a suitable scale for the model radomes. It was then used to evaluate inviscid flow trends for various speed and attitude configurations.

The experimental work was carried out in the Carleton University incompressible flow closed-circuit wind-tunnel. A 45 kW induction motor powers the wind-tunnel

fan and a magnetic clutch controls the fan's rotation speed. The test-section dimensions are 30" $\times$ 20" in cross-section and 72" in length. Experimental work was required as viscous effects were expected to be significant. Reynolds number effects were not expected to be significant, since the boundary-layer upstream of the forward radome was tripped and artificially thickened to ensure fully turbulent flow. Since neither full-scale nor model were subject to transition from laminar to turbulent flow, the experiment was not expected to suffer unduly from Reynolds number effects.

Chapter 2 examines the wake survey method of drag determination, compiles previous studies on trailing horseshoe vortices at the base of surface-mounted bluff bodies in a boundary-layer flow, and introduces the panel method code, PMARC. Chapter 3 describes the experiment. Model design, preliminary panel method computations, the apparatus and calibration procedures are presented, and methods for determining aerodynamic forces and moments of interest are explained. Chapter 4 presents and discusses the computational and experimental results. Chapter 5 summarizes the important findings and provides some recommendations to improve future related studies.

Chapter 2

Background Information

This chapter provides some background information to complement the present study. Details of the wake survey method used for total radome drag determination are described. Previous research into horseshoe vortex formation at the base of wall-mounted bluff bodies is presented. In particular, results of recent experimental studies are reported, and salient features of horseshoe vortices as determined from flow visualization experiments are described. Finally, the panel method entitled PMARC (Panel Method Ames Research Centre), used for the computational work in this thesis, is introduced and discussed.

2.1 Wake Survey Method

Using a control-volume analysis and applying the integral form of the conservation of momentum principle, the total drag, D_t , on any body immersed in an incompressible fluid flow can be obtained from the velocity distribution in the body's wake according to:

$$D_t = \rho \iint_{wake} u(U_\infty - u)da \quad (2.1)$$

where u denotes the velocity distribution in the wake, U_∞ is the undisturbed freestream velocity and da is a wake cross-section area element (Schlichting, 1968). The integration must be carried out over the entire wake. The measurement plane over which Equation 2.1 applies must be sufficiently far downstream of the body that

the static pressure in the plane is equal to the static pressure in the undisturbed stream. When the flow field is constricted, however, such as a flow bounded by the walls of a wind-tunnel, the change in freestream static pressure due to model and wake blockage and boundary-layer formation at the wind-tunnel test-section walls must be accounted for. Betz (1925) was the first to calculate this pressure correction term. A detailed analysis of Betz's derivation for profile drag as obtained from wake surveys can be found in Schlichting (1968) and will not be reproduced here. Using a control-volume analysis as above, Betz determined the profile drag, D_p , of a body immersed in a fluid flow, as:

$$D_p = (H_o - P_o) \iint_{wake} \left(1 - g - (\sqrt{1-p} - \sqrt{g-p})(2 - \sqrt{1-p} - \sqrt{g-p})\right) da \quad (2.2)$$

$$\text{where } g = \frac{(H - P_o)}{(H_o - P_o)} \quad \text{and} \quad p = \frac{(P - P_o)}{(H_o - P_o)}$$

where H_o and P_o are respectively the total and static pressures in the undisturbed stream, and H and P are the corresponding quantities at the elementary area, da , in the measurement plane. One important advantage of Betz's equation is that the integral is written in terms of flow variables that vanish outside the wake. Thus, in a wind-tunnel experiment the probe traverse area may be limited to the viscous wake in the measurement plane.

When calculating aerodynamic forces from wake surveys, the following conditions must be observed (Brune, 1994):

- Wake flow data are measured in a single plane downstream of the body: this plane is perpendicular to the wind-tunnel axis.
- The flow at the wake survey station and far upstream of the model is steady and incompressible. This does not pose a limitation on the 'compressibility' of the flow in the vicinity of the model (i.e. the flow may be 'locally' compressible).
- The flow in the wind-tunnel is a uniform freestream parallel to the wind-tunnel axis.

- To account for the axial pressure gradient in the wind-tunnel test-section, the undisturbed flow conditions such as freestream total and static pressures are taken to be those measured in the freestream at the wake survey station (i.e. in the inviscid core, outside the model's wakes and wall boundary-layers), rather than upstream of the model.

Wake surveys are typically time consuming since a large number of data points are required to adequately describe the wake. Brune (1994) conducted a wake survey downstream of a vertically mounted half-wing model of a twin-engine transport in Boeing's 8- by 12-ft. transonic wind-tunnel. In this experiment, 15,000 wake points were required to capture the wake. Accurate data were obtained with a probe traversing speed of 1 in/s, and the experiment took two hours to complete. Locke et al. (1996), who performed wake surveys of yacht sails in a wind-tunnel, reported the same drawback to the wake survey method. The advantage of the method lies in the information it provides about the structure of the flow. As will be seen in this thesis, the wake surveys effectively capture the horseshoe vortices at the base of the radomes, as depicted in contour plots of momentum-flux-deficit coefficient.

A more general form of Equation 2.2 includes a second integral containing cross-flow velocities. Maskell (1972) showed that this integral, limited to the viscous part of the wake, represents the induced drag, D_i , of the body. Separate measurements of D_i and D_p are of value to the developer of CFD codes since both components are associated with different phenomena which must be evaluated separately (Brune, 1994). Through a similar control-volume analysis, lift can also be derived from wake survey data. Studies have shown that lift obtained from wake surveys agrees very well with both inviscid theoretical predictions of panel codes and balance measurements (Brune and Hallstaff, 1985). Uncertainties in lift measurements include a shift of the vortex filaments between the model and the wake survey plane (which distorts the spanwise load distribution), dissipation of the wake vorticity, and the inability to distinguish between lift and side forces.

In this thesis, only the *total* drag was of interest, so no attempt was made to separate the total drag into induced drag and profile drag from wake surveys. Also,

lift was determined from surface static pressure measurements on the model rather than from wake surveys. Measurements of both induced drag and lift would have required crossflow velocity measurements at the wake survey station. Lift on the radomes would be relatively small and it would not have been possible to measure crossflow angles with sufficient accuracy to yield meaningful lift data using this approach.

2.2 Flow Structure of a Turbulent Junction Vortex

It is often observed that, at the base of surface-mounted bluff bodies, the approach-flow undergoes a three-dimensional separation. The lower regions of the separated flow roll up to form a vortex structure ahead of the body. This vortex structure, known as the ‘junction vortex’ or ‘horseshoe vortex’, trails downstream on either side of the body. It was expected that horseshoe vortices would form at the base of the radomes in the present experiment. The essential features of their structure, as described in the literature on the basis of velocity field data, surface oil-flow visualization and surface pressure measurements, are presented in this section.

Various vortex structures upstream of surface-mounted bluff bodies are reported by different researchers. Hunt et al. (1978), Baker (1980), and Ishii and Honami (1986) all observed a four-vortex plane of symmetry flow field model; that is, two pairs of counter-rotating vortices in the plane of symmetry upstream of the body (see Figure 2.1). Kinematic considerations require that vortices occur in pairs (Hunt et al. (1978)). Baker inferred the vortex pattern in Figure 2.1 from surface oil-flow visualization experiments, which showed two pairs of separation and attachment lines crossing the line of symmetry upstream of the body. Vortex 1, the main or primary vortex, dominates the flow. Vortex 2, a smaller-scale vortex, is seen upstream of the primary vortex. Vortex 0 is caused by separation of the boundary layer on the upstream face of the body and vortex 1’ is caused by separation of the primary vortex.

Figure 2.1: Junction Vortex Flow Pattern (taken from Baker (1980))

Pierce and Harsh (1988), Pierce and Tree (1990), and Bain and Fletcher (1993) all reported a 2-vortex flow structure upstream of surface-mounted bodies having cylindrical and streamlined-cylindrical shapes. The 2-vortex model is of particular interest, since this flow structure is observed in the present study. It consists of one large primary vortex and one small tightly-wound body/floor corner vortex of opposite sign. Figure 2.2 presents surface oil-flow patterns at the base of a streamlined cylinder, observed by Pierce and Tree (1990). Note that the experiments from the Pierce and Tree study were performed at Reynolds numbers of comparable magnitudes to those in the present study. Figure 2.2 shows a primary separation point on the line of symmetry at approximately one cylinder radius upstream of the body. A primary separation line consisting of a series of ordinary separation points crosses the line of symmetry at the primary separation point and wraps around the body. This primary separation line is approximately equidistant from the body over the first 90° turning angle from the leading edge. Just downstream of the primary separation line is a 'distinct' line which crosses the line of symmetry and also wraps around the body. Steinhauer (1965), Belik (1973), and Baker (1979, 1980) all agree that this distinct line is a projection of the primary vortex centre on the floor. The

region bounded by the distinct line and the body is typically devoid of oil due to high surface shear stresses. The shear stresses fall to zero near the primary separation line, where the approach flow meets with the reverse flow of the primary vortex, as evidenced by an accumulation of oil-pigment mixture along this line.

Figure 2.2: Surface Oil-Flow Pattern Upstream of Cylinder (taken from Pierce and Tree (1990))

Pierce and Harsh (1988) reported that floor static pressure measurements upstream of the body indicated a relative local high very close to the primary separation point, and a locus of relative minima very close to the distinct line. A non-intrusive Laser Doppler Velocimetry (LDV) system used in the Pierce and Tree (1990) study provided two-dimensional velocity measurements in the plane of symmetry. The velocity field in the plane of symmetry showed the primary vortex clearly, but the body/floor corner vortex could not be distinguished. The crossflow vorticity field, however, computed from the velocity field, depicted a small corner vortex. Velocity

fields in planes perpendicular to the flow at the 43% and 100% chord positions of the streamlined cylinder confirmed that the 2-vortex structure trailed downstream on either side of the body. Between the two measurement planes, it was also noted that the primary vortex grew in size and decreased in streamwise vorticity by 63%.

A survey of recent studies of low-speed turbulent junction flows indicates that the number of vortices increases with increasing Reynolds number. In particular, results of six studies, summarized briefly in Pierce and Tree (1990), show that in general, 4-vortex systems exist for $R_d > 10^6$ and 2-vortex systems exist for $R_d < 10^6$. It is worth noting that Sidney and Kitchens (1975), who performed flow visualizations for obstacle/boundary-layer interactions in supersonic turbulent flows, presented junction vortex systems with 2, 4, and 6 vortices with corresponding multiple separation-attachment line pairs.

As mentioned earlier, the two-vortex system was observed at the base of the radomes in this study. This was deduced from oil-flow visualization experiments, which are presented in Chapter 4.

2.3 Description of PMARC

A panel method code called PMARC (Panel Method Ames Research Centre) was used to carry out the computational work in this thesis. The FORTRAN 77 source code, based on inviscid and incompressible (potential) flow theory, numerically predicts aerodynamic flows about complex three-dimensional bodies. It was derived from VSAERO, a panel code written over a ten-year period by Analytical Methods Inc. PMARC has an open architecture which permits the addition of subroutines to satisfy contract- or industry-specific requirements. Adjustable-size arrays controlled by a set of parameter statements at the beginning of each subroutine permit the user to tailor variables (such as the number of surface panels for instance) to satisfy their particular needs. PMARC capabilities include internal-flow modelling for ducts and wind-tunnel test sections, jet plume modelling for the analysis of powered-lift aircraft, and a time-stepping wake model for unsteady flows.

PMARC is a low-order panel method, and as such, the singularity distribution

is assumed constant at the panel-centroid value for each panel. Higher-order panel methods claim better accuracy, as singularity distributions vary linearly or quadratically over each panel. However, low-order panel methods are proven to be nearly as accurate and much more practical in terms of computation time, code simplicity, and cost efficiency. PMARC solves the governing equations for the velocity potential function by using a distribution of sources and doublets at the panel centroids and applying the flow tangency condition at these points.

The surface geometry is divided up into ‘patches’, for which the number of panels in each row and column are user-defined in the PMARC input file. A sample input file is presented in Appendix A. The PMARC output file provides a geometrical data block which contains cartesian coordinates of panel centroids and panel corner points, as well as fully-defined panel unit normal vectors. The aerodynamic block consists of doublet strengths, pressure coefficients, three-dimensional velocity components, and Mach numbers, all defined at panel centroids and/or panel corner points. An off-body analysis provides these flow variables at arbitrary points in the flow field. Forces and moments are computed for each patch by integrating the pressure coefficient distributions in the appropriate directions. PMARC uses linearized theory and the first Prandtl-Glauert rule to correct pressure coefficients, forces and moments for compressibility effects. Saad (1993) provides details of the Prandtl-Glauert rules.

In this study, preliminary panel method computations were performed to investigate wall interference and model blockage effects for different model scales, and to select an optimal model size. Subsequent panel method computations demonstrated inviscid flow trends for various speed and attitude configurations.

Chapter 3

Description of Experiment

This chapter begins with an outline of the wind-tunnel model design. Preliminary panel method computations used to examine wall interference and model blockage effects, and to choose a suitable model scale, are then presented. The chapter proceeds with a depiction of the Pitot/static probe assembly, wake traverse gear, pressure transducers, data acquisition module, SCANIVALVE/wafer switch, and data acquisition software. The calibration work is then outlined. This includes calibration of the pressure transducers and wind-tunnel test-section, and a description of the approach-flow boundary-layer characteristics. Finally, methods for determining the aerodynamic force and moment coefficients of interest in this research are explained.

3.1 Model Design

Three main restrictions limited the size of the model radomes. The first restriction was that the scale had to be small enough to allow wake surveys to be taken sufficiently far downstream of the aft-radome that substantial reverse flow would not be encountered. The second restriction was that the tunnel wall interference effects should be negligibly small. The third and final restriction was that of having the Reynolds number of the wind-tunnel flow as close as possible to that of the full-scale flow. With a maximum forward-radome width selected from preliminary panel method computations of 6.9 in., and for a maximum wind-tunnel speed of 230 ft/sec,

a Reynolds number of 1/11 of full scale could be achieved in the wind-tunnel. The full scale Reynolds number is that calculated for Mach 0.7 cruise at 36,000 ft. the flight condition of primary interest. The degree of smallness of the radomes was also limited by convenience of installation; that is, the ability to easily install the many pressure taps and associated PVC tubes required for static pressure measurements on the surface of the radomes.

The wind-tunnel set-up was first designed in a graphical software package called IDEAS (Integrated Design Engineering Analysis Software). The various components of the set-up included the forward- and aft-radomes, fairing ribs, fairing skin and base plate. Each was designed separately in IDEAS. The designs were then sent for manufacturing at the Science and Technology Centre at Carleton University, where they were manufactured by a six-axis CNC machine, which used MasterCam as its programming tool. Software compatibility between IDEAS and MasterCam enabled a file transfer process of the design files using the IGES format representation.

3.1.1 Radome Design

Information regarding radome configuration was provided by the Department of National Defence (DND). DND supplied CAD drawings containing cross-sectional contours of the radome exteriors at various axial stations along the underside of the fuselage. Coordinates of these contours were picked off using a digitizing tablet. The points picked off by the digitizer for each contour corresponded to angular positions of 0, 20, 40, 60, 80 and 90 degrees, relative to a plane perpendicular to the plane of symmetry of the aircraft and tangential to the underside of the basic fuselage cylinder. This plane will be referred to as the “baseplane” in the remainder of the thesis. Note that due to the symmetrical nature of the radomes, it was necessary to digitize the curves on only one side of the longitudinal plane of symmetry, since the other side could be mirrored in the radome design phase.

With two-dimensional coordinates of the full-scale contour curves and their respective axial positions along the fuselage known, the information was input into IDEAS. Three-dimensional splines were used to connect the cross-sectional contours,

producing surfaces which took the shape of the radomes. To create the internal cavity for the model radomes, the radome exterior surfaces were then scaled to approximately 90% of their size. The exterior and internal surfaces were then merged to define the shells of the model radomes, with a uniform thickness of approximately one inch. A rendered picture of the forward-radome is shown in Figure 3.1. The image shows both solid and wireframe geometry, where one side of the outer surface of the radome is transparent. The full-scale and model radome dimensions are shown in Table 3.1. The model radome scale factor is 0.145. The basis for selecting this scale is outlined in Section 3.2.

Table 3.1: Full-Scale and Model Radome Dimensions

Dimension [in/cm]	Forward-Radome		Aft-Radome	
	Full Scale	Model	Full Scale	Model
Length	86.6/220	12.6/31.9	80/203.2	11.6/29.5
Width	48.5/123.2	7.0/17.9	54.8/139.2	7.9/20.2
Height	22.6/57.4	3.3/8.3	22.1/56.1	3.2/8.1

For lift and pressure-drag measurements, the radomes were fitted with static pressure taps. 1/16" holes were drilled at specific locations along one side of each radome's plane of symmetry. They are shown as the cross-hairs in Figure 3.1. Due to the symmetrical nature of the model, it was not necessary to fit static pressure taps on both sides of the radomes. Brass tubes of 1/16" OD and 1/32" ID were inserted into the drilled holes and glued such that their tips were flush with the outer surfaces of the radomes. 3/32" OD, 1/32" ID PVC tubing was fitted to the exposed brass tubes on the inside of the radomes. The specific locations of the static taps corresponded to panel corner points as determined from the PMARC output deck (see Section 3.1). Fifty-two static taps were installed on the forward-radome, and twenty-seven on the aft-radome.

Figure 3.1: Forward-Radome

3.1.2 Wing/Body Fairing and Base Plate

The transverse curvature of the fuselage, on which the actual radomes are mounted, could not be modelled in the wind-tunnel because of the rectangular geometry of the test-section. Consequently, it was decided that the flat floor of the wind-tunnel test-section would represent the baseplane (see Section 3.1.1 for definition of the baseplane). Only that portion of the actual radomes which protrudes below the base plane was thus modelled in the wind-tunnel experiment.

Protruding below the basic cylinder of the aircraft's fuselage and blending with both radomes is a wing/body fairing. The fairing was modelled as a two-dimensional shape, its height above the wind-tunnel floor corresponding to the distance between the fairing surface and the surface of the basic fuselage cylinder at the aircraft's plane of symmetry. Information regarding the fairing shape came from the CAD drawings provided by DND. While the drawings did not provide explicit information regarding the fairing's shape, especially between the radomes, a reasonable fairing shape could be inferred from the shown intersection points between radome and fairing contours. It is reasonable to assume that the flow is insensitive to details of the fairing shape.

The model fairing skin was made of 0.25 in. thick PVC sheet. Structural support for the fairing skin was provided by four fairing ribs, evenly spaced across the width of the test-section. The skin incorporated cutouts to accommodate the model radomes. Ten static pressure taps were fitted along the fairing centreline, between the two radomes, at locations corresponding to panel corner points from the PMARC output file. Four fairing skins were manufactured; one for each radome separation distance and one for the no-radome test case. This latter configuration was used to determine basic skin friction drag. The forward-radome's leading edge position was fixed at six inches from the test-section inlet, and the aft-radome location varied according to the chosen radome separation distance.

The radomes and fairing ribs were fastened from underneath to a 72" $\times$ 30" $\times$ 1/4" PVC base plate, which spanned both the width and length of the test-section floor. The leading edge of the 1/4" base plate was beveled to provide a streamlined contour

for the flow into the test-section. The base plate was attached to the test-section floor with a number of flat head machine screws, fastened with washer and nut combinations from underneath the floor. The surface of the base plate was the effective test-section floor, which corresponded to the “baseplane” as defined in Section 3.1.1.

Details of the model surface geometry can be found in Appendix B.

3.2 Preliminary Panel Method Computations

Preliminary panel method computations were performed to investigate wall interference and model blockage effects for different model scales, and to select an optimal model size. A radome size was chosen based on a compromise between minimum wall interference effects and maximum feasible Reynolds number. As mentioned in Section 3.1, the scale had to be small enough to permit wake surveys sufficiently far downstream of the aft-radome to avoid possible regions of reverse flow.

To carry out preliminary computations, the wind-tunnel set-up had to be modelled in PMARC. The required geometrical information came from IDEAS. The two radomes, fairing skin, fairing ribs and base plate, all designed individually, were assembled in IDEAS. The wind-tunnel set-up for the design radome separation distance is shown in Figure 3.2. To model the geometry of the radomes and fairing for PMARC, coordinates were picked off from their outer surfaces. The intersections between the radomes and the fairing were found using a surface intersection feature in IDEAS, and the coordinates of those intersection lines were used in the PMARC input deck.

In this manner, the entire geometry could be represented, including the walls and the ceiling of the wind-tunnel test section. Only one side of the model’s plane of symmetry was input into PMARC. The mirror feature was selected in the input deck to produce force and moment coefficients based on lengths and areas of the complete geometry. PMARC calculated aerodynamic coefficients by normalizing all forces with respect to the three-dimensional surface area of the forward-radome, S_{for} . Lift and drag coefficients were defined as follows:

Figure 3.2: Wind-Tunnel Model

$$C_l = \frac{L}{\frac{1}{2}\rho V_\infty^2 S_{for}} \quad \text{and} \quad C_d = \frac{D}{\frac{1}{2}\rho V_\infty^2 S_{for}} \quad (3.1)$$

To investigate the interference and blockage effects, three model scales were simulated in PMARC. They were 11.6%, 14.5%, and 17.4% of full scale. A free-air test case was also simulated in PMARC. The pressure distributions over the entire experimental set-up are presented graphically in Figures 3.3 to 3.6, for each of these cases. For clarity, the near wall and part of the ceiling are not shown in the first three of these figures. Lift, drag and minimum pressure coefficients of the forward- and aft-radomes, obtained from PMARC, are shown in Table 3.2.

Table 3.2: Preliminary Force and Minimum Pressure Coefficients over Model Radomes

Model Scale [%]	Forward-Radome			Aft-Radome		
	C_l	C_d	C_{pmin}	C_l	C_d	C_{pmin}
11.6	0.0666	0.0000	-0.5828	0.0824	-0.0058	-0.6166
14.5	0.0663	0.0005	-0.5799	0.0823	-0.0063	-0.6154
17.4	0.0658	0.0011	-0.5743	0.0804	-0.0070	-0.6064
Free-Air	0.0633	-0.0011	-0.5718	0.0781	-0.0049	-0.5971

As shown in Table 3.2, the lift coefficient decreases with increasing model scale. Theoretically, the opposite is true. Increasing the model size in a fixed-boundary wind-tunnel decreases the flow area and increases the velocity over the radomes to produce greater lift. The differences in lift coefficient are small, however, and are attributed to the constant panel numbering for all three model scales. Note that PMARC is a first-order panel method, and as such, the pressure determined at a panel centroid is assumed constant over that panel. To examine the effect of panel numbering more closely, the number of panels on the forward- and aft-radomes were doubled for the 14.5% model scale test case. As a result, the minimum pressure coefficient, C_{pmin} , of the forward-radome decreased from -0.5799 to -0.6140

Figure 3.3: Computational C_p Contours in Wind-Tunnel; 11.6% Scale

Figure 3.4: Computational C_p Contours in Wind-Tunnel; 14.5% Scale

Figure 3.5: Computational C_p Contours in Wind-Tunnel; 17.4% Scale

Figure 3.6: Computational C_p Contours over Radomes; Free-Air

(-5.9%). While the decrease is not substantial, it demonstrates the significance of panel density. Note that since potential flow theory predicts zero drag on a body immersed in a flow, the non-zero drag coefficients in Table 3.2 provide an estimate of the error associated with the numerical convergence of the PMARC panel code.

The most notable effect of the test-section walls on the flow field was observed by running the panel method without any walls, for comparison. The $C_{p_{min}}$ of the forward radome for the free-air test case was -0.5718 (1.4% greater than the $C_{p_{min}}$ for the 14.5% model scale test case). This relatively small difference is representative of the slight wall interference and model blockage effects present in the wind-tunnel. To minimize these effects, all experimental pressure measurements were normalized with respect to local freestream pressures (i.e. freestream pressures in the measurement plane). This is described in detail in Section 3.5 (*Methods for Determining Aerodynamic Forces and Moments*).

Since the three model scales showed similar results, it was decided that the most suitable model scale was 14.5%. This scale permitted sufficiently small radomes without compromising convenience of installation or accuracy of experimental measurements. It also allowed wake surveys to be taken far enough downstream of both radomes without encountering regions of reverse flow. This was especially important as three radome separation distances were tested and the aft-radome could not be too close to the breather section at the downstream end of the wind-tunnel test-section for the far-apart test case.

It is important to note that while these preliminary computations provided a means for selecting the optimal radome scale, the results obtained did not accurately represent the nature of the flow over the radomes, which is dominated by viscous effects. The panel method is an inviscid code, for which all geometrical boundaries become streamlines of the flow. The boundary-layer formation at the test-section walls and on the model is not modelled in PMARC, so the attendant decrease in streamwise static pressure is not shown. Nor is PMARC capable of modelling flow separation or wakes of the radomes. However, the results presented are sufficiently accurate for the present purpose of analyzing the wall interference and model blockage effects and selecting a suitable model scale.

3.3 Apparatus

3.3.1 Pitot/Static Probe and Wake Traverse Gear

Wake surveys were taken using a Pitot and static probe assembly, which was traversed in a vertical plane by an existing wake traversing gear. This two-axis computer-controlled traversing gear was fastened to the wind-tunnel test-section ceiling-panel, outside of the tunnel. The probe assembly was traversed in a plane normal to the streamwise direction. Two planes were traversed; one behind the forward-radome and the other behind the aft-radome. They are labeled as planes I and II, respectively, in Figure 3.7.

The probe assembly was manufactured from aluminum tubing of various diameters. A standard Pitot probe was not used because the available lengths were too long. For this experiment, a tube length was selected based on the vertical traverse distance, which was chosen to be a maximum of 8 in. from the base plate. Rather than designing and manufacturing a Pitot/static probe with two concentric tubes and total and static pressure interfaces, a simpler scheme was devised. This consisted of two tubes separated by 0.75 in., one to measure total pressure and the other to measure static pressure. A schematic of this arrangement is shown in Figure 3.8.

The two-axis wake traverse gear was used to move the probe throughout the two measurement planes. Figure 3.9 shows the wake traverse gear fastened to the test-section ceiling-panel, as viewed from upstream of the measurement planes. A slot having a length equal to the spanwise traverse distance and a width of just over $3/8$ " (the diameter of the stem rod) was made in the ceiling-panel at both of these measurement planes. To prevent any suction or blowing through the slots during a wind-tunnel run, foam was taped along their lengths on both sides, minimizing the open area. Each axis of the traverse gear was computer-controlled by a stepping motor through a controller/interpreter unit (see Section 3.3.2). A six-wire Superior Electric Slo-Syn motor rated at 10.1 V and 0.5 A was used for the horizontal traverse, while an eight-wire Eastern Air Devices motor rated at 5.1 V and 1.0 A was used for the vertical traverse. A threaded rod fastened to the horizontal-axis motor

Figure 3.7: Location of Measurement Planes in Wind-Tunnel

Figure 3.8: Schematic of Pitot/Static Probe Assembly (all dimensions in inches)

moved the centre block, through which the stem rod passed. Another threaded rod, fastened to the vertical-axis motor, moved the middle block, to which the stem rod was attached.

Figure 3.9: Schematic of Wake Traverse Gear

A slight modification was made to the vertical traverse mechanism. Due to the necessity of traversing a longer vertical distance than was provided for in the existing traverse gear design, extensions to the stem and vertical guide rods were made. New rods four inches longer were manufactured. The modified traverse gear provided a horizontal traverse range of 5 in. on either side of the test-section centreline and a vertical traverse range of 8 in. from the base of the model. A small aluminum fitting with two thru holes was used to fasten the Pitot/static probe assembly to the stem rod. An offset fitting was used in some test runs to traverse the probe assembly closer to the test-section wall than the 10 inches allowed by the normal setup.

3.3.2 Instrumentation and Data Acquisition System

The experiment was divided into two phases: wake surveys in planes I and II for drag determination, and surface static pressure measurements for lift and pitching moment determination. A Turbopascal program, entitled Real Time Flow Analysis (RTFA), written previously for other research (Pajayakrit, 1997), was suitably modified and used in the present study. The program controlled the probe location and movement in the wind-tunnel, and recorded all data. The wind-tunnel runs were entirely automated. In this section, the experimental set-up, including the instrumentation and data acquisition unit, are described for both phases of the experiment.

Phase I: Wake Surveys

For wake surveys, the traversing gear was programmed to position the probe throughout the measurement planes. The probe moved a pre-set distance, waited for a pre-set waiting time, sampled a pressure for a pre-set sampling time, then repeated the procedure. Section 3.4.3 describes the selection procedure for optimal waiting and sampling times. At each probe position, both static and total pressure measurements were recorded. A waiting time between the two readings was set to permit the air pressure in the tubes to adjust to the pressure felt at the probe's measurement interfaces. Tygon tubing connected the probe assembly's static and total pressure

tubes to a wafer switch. The wafer switch was used to step between the two pressure readings. The single output of the wafer switch was connected via a Tygon tube to one of two Setra Systems Model 239 ± 0.5 psid pressure transducers. The other port of the pressure transducer was connected to a static pressure tap near the downstream end of the wind-tunnel contraction. This pressure is designated as P_{c_2} . The second pressure transducer monitored the difference between the upstream and downstream contraction pressures, or $(P_{c_1} - P_{c_2})$, which is directly proportional to the dynamic pressure in the wind-tunnel test-section. This relation will be described in more detail in Section 3.4.2. The electrical output of the pressure transducers was converted from an analog to a digital signal by a Sciometric Instruments Model 81 Electronic Measurement System. The digital signals were recorded by the computer, and presented in real-time as voltages on a monitor. For every probe pressure measurement, $(P_{c_1} - P_{c_2})$ was recorded simultaneously. A schematic of the experimental set-up for phase I is shown in Figure 3.10.

Signals to step the wafer switch came from the controller/interpreter, constructed in-house for previous research at Carleton University (Pajayakrit, 1997). This unit was connected to the computer through a 25-pin data bus cable. It sent digital signals to a CTRL2 Solenoid Controller, which stepped the wafer switch between the static and total pressure ports.

The two pressure transducers provided a nominal output of -2.5 to 2.5 VDC for input pressures ranging between -0.5 and 0.5 psid, and required 24 VDC excitation. They both had a quoted nonlinearity of $\pm 0.1\%$ FS and a zero-pressure output of $0 \text{ VDC} \pm 0.4\%$ FS. The transducers were re-calibrated for this experiment, and the present calibrations were used to reduce the wind-tunnel data. The transducer calibration method is described in Section 3.4.1.

The Sciometric Instruments data acquisition system required a 12 VDC power supply for operation. It was connected to the computer via an RS232 serial interface card and a Sciometric Instruments CPU Module. The data acquisition system had eight analog channels, three of which were used for this experiment. Two were used for the electrical output of the two transducers, and the third was used for auto-zeroing purposes. This latter channel used a wire to short the high and low inputs

Figure 3.10: Experimental Setup for Phase I

together. The software was programmed to measure the voltage present on this channel once every minute, and to subtract this 'zero' voltage from all subsequent voltage measurements to compensate for any offset-voltage drift.

The RTFA code was programmed to use the data acquisition system's 'differential voltage' option, representing the type of analog input signal. Before the DC voltage was converted to a digital signal, it was passed through a software programmable variable gain differential amplifier. There were four ranges of voltage measurements to choose from. The maximum pressure readings in the wind-tunnel corresponded to voltage values in the vicinity of 1.5 V. This dictated choice of a voltage gain value corresponding to a full scale differential output of ± 5.0 V. For this range, the weight of the least-significant A/D bit was 1.22 mV. This represented the accuracy to which the measurements were resolved by the data acquisition system.

Analog measurements were read by a 12 bit-plus-sign, dual slope, integrating Analog-to-Digital (A/D) converter. The A/D converter yielded a digital value between 0 and 4095, a sign-bit and an over-range bit as output for each conversion. The value represented the measured voltage, the sign-bit indicated whether the voltage was positive or negative, and the over-range bit indicated if the value was out of the software-set voltage range. The A/D converter was of the integrating type, which meant that the voltage value was integrated over a period of time. A factory-set integration period of 1/30 sec. permitted an A/D conversion speed of approximately 10 conversions/second (7.5 conversions/second at minimum). The computer's inability to capture that same conversion frequency resulted in a sampling rate of about 6 samples/second.

Phase II: Surface Static Pressure Measurements

Most of the instrumentation used for surface static pressure measurements was the same as for the wake surveys. The only difference was that a 48 port SCANIVALVE was used instead of a wafer switch due to the large number of static taps on the model. In addition to the static taps on the model itself, seven static taps were installed in the test-section ceiling along its centreline, with equal axial spacing, to

monitor the local freestream static pressure. Holes were drilled through the base plate and tunnel floor underneath both radomes and the fairing skin to pass the PVC pressure tubes out to the SCANIVALVE. The 44 tubes from the forward-radome were numbered and connected to a male SCANIVALVE connector. The 27 tubes from under the aft-radome, 10 from under the fairing skin, and 7 from the test-section ceiling, were numbered and connected to another male SCANIVALVE connector. 16-AWG PVC tubes were connected to each of the 48 SCANIVALVE ports at one end and gathered by a female SCANIVALVE connector at the other. The female and male SCANIVALVE connectors were coupled to complete the connection between the static taps and the SCANIVALVE. Since there were two male SCANIVALVE connectors, one manual switch between the female and male connectors had to be made to gather all the pressures in the wind-tunnel during an experimental run.

The single output tube of the SCANIVALVE was sent to one of the differential pressure transducers, which was referenced to P_{c_2} . The second pressure transducer measured $(P_{c_1} - P_{c_2})$. The data acquisition unit converted the analog output of the pressure transducers into digital signals, which were read by the computer and displayed on the monitor as voltages.

The controller was used to send signals to the SCANIVALVE to step from port to port. The data recording process was entirely automated. The computer was programmed to step to a port, wait a prescribed waiting time, sample a prescribed sampling time, and repeat the procedure. Section 3.4.3 describes the selection procedure for optimal waiting and sampling times. $(P_{c_1} - P_{c_2})$ was recorded simultaneously with each static pressure.

3.4 Calibration Work

3.4.1 Pressure Transducer Calibrations

The Setra pressure transducers used in the experiment were calibrated prior to the wind-tunnel tests. Intermittent checks were made throughout the testing period to

ensure that they were functioning properly. They were calibrated individually by blowing air from a squeeze-bottle through a Tygon tube, where a three-way tube connector was used to direct the air to one end of a U-tube manometer and to one input of the pressure transducer. The free ports of the U-tube manometer and of the pressure transducer were left open to atmosphere. A squeeze-bottle was used instead of blowing human air through the tube, since the human breath contains high levels of humidity. The electrical output of the transducers was connected to a Digital Fluke Multimeter 8080A.

By applying air pressure from the squeeze-bottle, both the manometer fluid deflection and transducer output voltage were recorded simultaneously. The applied pressure was maintained by folding the manometer tube to trap the air and prevent fluid deflection long enough to record the two values. Successive pressures applied in this manner permitted a pressure-voltage curve to be drawn. A best-fit straight line using the least-squares method was used to fit the resulting data points. The accuracy of this calibration technique was limited by the fact that the fluid deflection could not be resolved to better than 0.25 mm H_2O (2.4 Pa). This uncertainty in the manometer reading was factored into the final wind-tunnel results.

Figure 3.11 shows the pressure-voltage relationship for the two Setra pressure transducers. As mentioned earlier, intermittent checks were made throughout the testing period, and the resulting data points are shown on the graphs. The Setra pressure transducers were somewhat sensitive to ambient temperature and humidity. The electrical output of the transducers tended to be higher on hotter days. As can be seen from Figure 3.11, there was a distinct shift in the data points dated Oct 2, when the ambient temperature was cooler than in the summer. The procedures used for referencing and normalizing the pressure data ensured that the reduced data are not affected by the small changes in the transducer sensitivity.

The RTFA program had a provision for the zero-wind-velocity pressure reading. Immediately prior to each wind-tunnel test, a sample pressure was recorded with the Pitot/static probe assembly in position and the wind-tunnel fan off. During the course of a test run, the ambient temperature could rise significantly, thus affecting the output of the transducer. A zero-wind-velocity pressure reading was therefore

a) Setra Transducer 1

b) Setra Transducer 2

Figure 3.11: Calibration of Setra Pressure Transducers

recorded again at the end of the test run. The average of the two pressure readings was set as the zero-wind-velocity pressure reading, and was subtracted from all subsequent pressures measured throughout that particular test run.

3.4.2 Wind-tunnel Calibration

Calibration Constant

The dynamic pressure in the wind-tunnel is a function of the difference between the pressure upstream, P_{c_1} , and downstream, P_{c_2} , of the tunnel contraction. The relation is as follows:

$$\frac{P_{c_1} - P_{c_2}}{\frac{1}{2}\rho V_\infty^2} = K \quad (3.2)$$

where K is the calibration constant. The calibration constant is determined by traversing a Pitot probe throughout the test-section, near its inlet, and simultaneously recording $(P_{c_1} - P_{c_2})$ and the dynamic pressure. The average ratio of the two gives the calibration constant, which has previously been determined for the empty test-section as $K = 0.91$. Note that K changes axially throughout the empty test-section due to the boundary-layer formation at the walls. The reduced core flow area causes an increase in dynamic pressure and therefore a decrease in the calibration constant, for the same contraction pressure difference.

When a model is placed inside the test-section, the freestream dynamic pressure changes due to boundary-layer formation at the walls *and* blockage effects of the model and its wakes. It was shown in Section 3.2 (*Preliminary Panel Method Computations*), however, that proximity of test-section walls had little effect on the minimum pressure coefficient over the radomes. That is, with the chosen model scale, flow field constriction effects were minimal. Therefore, the dynamic pressure used to normalize surface static pressure measurements was taken to be that at the inlet of the test-section, where a modified calibration constant, due to the presence of the 1/4" base plate, was calculated. The modified calibration constant was determined from conservation of mass and area considerations as follows:

$$A_{new} V_{\infty new} = A_{old} V_{\infty old} \quad (3.3)$$

$$\left(\frac{V_{\infty old}}{V_{\infty new}} \right)^2 = \left(\frac{A_{new}}{A_{old}} \right)^2 \quad (3.4)$$

$$\text{Since } K_{old} = \frac{P_{c1} - P_{c2}}{\frac{1}{2} \rho V_{\infty old}^2} \quad (3.5)$$

$$\text{then } K_{new} = K_{old} \times \left(\frac{V_{\infty old}}{V_{\infty new}} \right)^2 \quad (3.6)$$

$$\text{so that } K_{new} = \frac{P_{c1} - P_{c2}}{\frac{1}{2} \rho V_{\infty new}^2}; \quad (3.7)$$

where A_{new} and A_{old} were the 'open' cross-sectional flow areas at the test-section inlet, with and without the base plate installed, respectively, and $K_{old} = 0.91$, the calibration constant of the empty test-section.

Wake survey pressure data were reduced using total and static pressure values measured in the freestream in the measurement plane. since such values were readily available by traversing the Pitot/static probe assembly outside the wakes and boundary-layer. Details are presented in Section 3.5.1.

Ceiling Static Pressure Distribution

As mentioned earlier, the variation in freestream static pressure along the test-section was obtained from static taps in the test-section ceiling. Figure 3.12 shows the measured ceiling static pressure distribution and a theoretical static pressure distribution based on conservation of mass and Bernoulli principles. A fourth-order polynomial was required to fit the measured pressures. Measurements were taken with the radomes removed and only the base plate and fairing installed, as it was found that the radomes had little influence on the ceiling static pressures. Note the full pressure recovery predicted in theory. The measured distribution shows less pressure recovery, due to the growing boundary-layer at the test-section walls.

The reduction in static pressure for axial positions between 10 and 50 inches is due to the fairing, which reduces the cross-section area of the flow. The measured ceiling static pressures at the axial station for any particular measurement were used

Figure 3.12: Ceiling Static Pressure Distribution along Wind-Tunnel Axis; Normalized by Dynamic Pressure at Test-Section Inlet

as the reference static pressure when reducing the surface static pressure data to coefficient form. This data reduction procedure should minimize the error due to the test-section blockage associated with the fairing. In the actual radome installation on the aircraft, there will of course be no blockage effects and the freestream static pressure will be uniform.

Spanwise Uniformity of Flow

It is important in wind-tunnel testing to ensure spanwise uniformity of the flow approaching the model. This was checked by traversing the Pitot/static probe assembly in the two measurement planes with the radomes removed and only the two-dimensional fairing skin and base plate installed. Boundary-layer measurements were taken at three spanwise locations: the wind-tunnel's centreline, and 5.2 in. (or 1.53 times the forward-radome half-width) left and right of the centreline. Figure 3.13 shows the results for measurement plane II. The good agreement between the three velocity profiles clearly indicates that the flow was indeed two-dimensional.

3.4.3 Selection of Optimum Waiting and Sampling Times

As mentioned earlier, waiting and sampling times were software controlled in both phases of the experiment. Given the large number of measurements involved, an objective prior to testing was to determine the optimum waiting and sampling times, to avoid unnecessarily lengthy wind-tunnel runs. One of the features of the RTFA program was that the computer monitor displayed all sample pressures over a prescribed sampling time at a fixed probe position, as well as their average value. This average value had to be close enough to the cumulative average of the samples to be deemed worthy. The program offered a visual check of the cumulative average of the samples. A real-time display was set up which showed each sample pressure plotted within a pre-defined range on the same line as its respective numerical value. One could see the value of the sample pressure as well as its position within the range. Instantaneous sample values were marked with an 'x' and cumulative average values were marked with an 'o' within the specified range. This graphical tool proved

Figure 3.13: Spanwise Flow Uniformity in Boundary-Layer; Measurement Plane

II

useful in selecting the optimum sampling time.

The waiting time was chosen based on a more direct measurement method. A Digital Fluke Multimeter 8080A was first connected to the pressure transducer output. Upon stepping between the static and total pressure ports at a fixed probe position, the time required for the transducer voltage reading to change from the static pressures to the total pressures was recorded. This time was between 2-3 seconds for a jump in voltage reading of approximately 2 V. A waiting time was thus conservatively set to 5 seconds. This proved sufficient for both phases of the experiment.

Figure 3.14 shows the instantaneous, cumulative average and overall average results of a total pressure measurement for three sampling times: 15, 20 and 25 seconds. The waiting time for each was 5 seconds. The measurement was taken in plane II at a spanwise distance from the wind-tunnel centreline of approximately 1.53 times the forward-radome half-width. The Pitot/static probe assembly tip was in contact with the floor. It was thus in a highly viscous region where substantial unsteadiness of the flow was expected. The radomes were at $0.8\times$ design radome separation.

As will be explained in detail in Chapter 4, a bi-stable wake pattern was observed behind the aft-radome at this radome separation. This accounts for the discrepancy between the mean total-pressure values of the three plots. From the scatter of Figure 3.14 c), (25 second sampling time), note how at sample no. 110, the total-pressure plummeted for a few samples, then began to return to the original scatter. This was a direct indication that the wake switched from one direction to the other for a short period of time, then returned to the original, more stable direction. In doing so, it brought the mean total-pressure down, and this explains the deviation between the mean and cumulative average of 0.3% over most of the samples. Figure 3.14 b), (20 second sampling time), shows how the wake direction switched to the unstable direction at sample no's: 10, 36, 51 and 85, giving lower total-pressure values at these points in time. Note how the mean is the lowest of the three for this case. This is due to the large number of the low total-pressure samples. For this case and that of Figure 3.14 a), (15 second sampling time), the

a) Waiting Time: 5s, Samp. Time: 15s

b) Waiting Time: 5s, Sampling Time: 20s

c) Waiting Time: 5s, Sampling Time: 25s

Figure 3.14: Test of Optimum Sampling Time

cumulative average and the mean were essentially the same over the last 10 samples.

On the basis of these and other measurements, a sampling time of 20 seconds was selected for both phases of the experiment. Note that the other two radome-separation distances did not exhibit the bi-stable wake condition, so the mean and cumulative averages for all pressure readings converged to the same approximate value relatively readily, even in the wake and boundary-layer regions, where greater flow unsteadiness was expected. A 20 second sampling time was thus entirely sufficient.

3.4.4 Approach-Flow Boundary-Layer Characteristics

For an accurate representation of the flow over the aircraft radomes, the characteristics of the boundary-layer on the wind-tunnel floor just upstream of the forward-radome should be similar to those of the boundary-layer at the corresponding position on the aircraft. On the aircraft, just upstream of the radomes, the flow is turbulent due to the long distance between the aircraft's nose and the leading edge of the forward-radome (26.6 ft.). A rough estimate of the laminar to turbulent transition point, x_{tr} , for flow over a flat plate, based on the one-step method of Wazzan et al. (White, 1991), for a flight characteristic of $M = 0.7$ at 36,000ft., is $x_{tr} = 2.9$ ft.

The boundary-layer in the wind-tunnel was thus tripped by placing sandpaper on the wind-tunnel floor at the downstream end of the contraction, immediately upstream of the leading edge of the base plate. This ensured a fully rough turbulent flow. To match the boundary-layer thickness at the leading edge of the forward-radome of the aircraft, $\delta_{for_a/c}$, with an appropriately scaled boundary-layer thickness at the corresponding position in the wind-tunnel, $\delta_{for_w/t}$, a simple power-law expression for turbulent flow was used (White, 1991):

$$\frac{\delta}{x} = \frac{0.16}{Re_x^{1/7}} \quad (3.8)$$

This yielded a *target* wind-tunnel boundary-layer thickness of $\delta_{for_w/t} = 0.61$ in., which is just under 1/5 of the forward-radome height. To establish the *actual*

wind-tunnel boundary-layer thickness, total pressure measurements were recorded as a function of vertical distance from the fairing at the wind-tunnel centreline in measurement plane I, with and without sandpaper as a tripping device. The velocity profiles deduced from the total pressure measurements for both these cases are shown in Figure 3.15.

Figure 3.15: Boundary-Layer Velocity Profile; Measurement Plane I

The boundary-layer thickness in measurement plane I, δ_I , was marked on a linear plot of δ vs. $x^{6/7}$, obtained from Equation 3.8. Knowing the distance between measurement plane I and the leading edge of the forward-radome, linear interpolation was used to estimate $\delta_{for_{w/t}}$. With one row of sandpaper, $\delta_{for_{w/t}} = 0.58$ in., deviating from the abovementioned target by 4.5%. While the two boundary-layer-thicknesses agree well, the present method was based on flat plate correlations, and is therefore not entirely accurate. However, a moderate mismatch in boundary-layer-thicknesses is not expected to have a substantial effect on the experimental pressure, force and

moment coefficients.

Of greater importance than boundary-layer-thickness matching is ensuring that the flow is fully turbulent. With one row of sandpaper installed, a shape factor, H , of 1.6 was calculated from the velocity profile data of Figure 3.15. This value is within the range attributed to turbulent flows. An artificial thickening of the boundary-layer is typically controlled by much larger stretches of sandpaper. Such flow alteration was not necessary and was not attempted in this experiment.

3.5 Methods for Determining Aerodynamic Forces and Moments

3.5.1 Total Drag Determination

The total drag produced by the radomes in the wind-tunnel was determined from a survey of their wakes. The momentum-flux-deficit in the wake is a measure of the total drag (Anderson, 1991). The total drag perturbation due to the radomes was obtained by subtracting the skin friction drag of the basic fuselage-surface model (i.e. without radomes) from the total drag measured with the radomes installed. Wake surveys in measurement planes I and II provided drag of the forward-radome and both radomes, respectively.

Betz derived the profile drag, D , of a body in an incompressible fluid flow using the momentum equation (Betz, 1925). His equation, presented in Chapter 2, is repeated here for clarity:

$$D = (H_o - P_o) \iint_{wake} (1 - g - (\sqrt{1 - p} - \sqrt{g - p})(2 - \sqrt{1 - p} - \sqrt{g - p})) da \quad (3.9)$$

$$where \quad g = \frac{H - P_o}{H_o - P_o} \quad and \quad p = \frac{P - P_o}{H_o - P_o};$$

H_o and P_o are the total and static pressures of the undisturbed stream, and H and P are the total and static pressures in the measurement plane downstream of the body.

Equation 3.9 was applied in the present experiment. The control volume consisted of a series of streamtubes which started at the inlet of the test-section and ended in the measurement plane downstream of the model. The measurement plane was divided into a number of grid points, at which total and static pressures were recorded. Each four adjacent grid points forming a grid 'panel' marked the end of a streamtube. The total and static pressures were assumed uniform over the panel and equivalent to the average of their four respective panel corner point values.

As mentioned in Section 3.4.2, to compensate for the slight effects of flow field constriction in the wind-tunnel, total and static pressures of the undisturbed stream, specifically H_o and P_o in Betz's formula, were taken to be the values of freestream total and static pressure in the measurement plane. That is, the total and static pressure found in the measurement plane, outside the wakes and boundary-layer, were used for H_o and P_o .

The measurement planes were located sufficiently far downstream of the radomes to ensure that they would not be in regions of reverse flow (see Figure 3.7 for location of measurement planes). This was checked by attaching a tuft to a hand-held wand and scanning the measurement planes behind each radome.

Since the lateral separation distance between the Pitot and static tubes was 0.75 in., it was decided to use that same distance as a step size when traversing the probe in the spanwise direction. In measurement plane II, the vertical step size was 1/8" for the first ten vertical measurements, 1/4" for the next ten vertical measurements and 1/2" for the final six vertical measurements. In measurement plane I, the step sizes were the same as in measurement plane II, but only two vertical measurements (instead of six) were taken at a step size of 1/2". The smaller step sizes were chosen to account for the greater variation in total pressure near the floor. A more detailed discussion of grid choice is deferred to Section 4.2.1 (*Grid Checks*).

A Fortran program entitled 'DRAG' was written to calculate the total drag (see Appendix C for program listing). The wind-tunnel data at each grid point consisted of a static pressure measurement, $(P_s - P_{c_2})$, a total pressure measurement, $(P_t - P_{c_2})$, and a reference dynamic pressure measurement, $(P_{c_1} - P_{c_2})$. As mentioned earlier, the averages of each of these measurements at four corner grid points surrounding

a panel were calculated, and the averages were assumed uniform over the panel.

The drag-coefficient increment for each panel was calculated from Equation 3.9. $(H_o - P_o)$ was taken as the local freestream dynamic pressure, that is, $[(P_t - P_{c_2})_\infty - (P_s - P_{c_2})_\infty]$. H_o was replaced by $(P_t - P_{c_2})_\infty$ and P_o was replaced by $(P_s - P_{c_2})_\infty$. The subscript ∞ denotes readings well outside the boundary-layer and wakes. H and P were replaced by the averaged $(P_t - P_{c_2})$ and $(P_s - P_{c_2})$ measurements, respectively, for each panel. The drag-coefficient increment, ΔC_d , for each panel is then given by:

$$\Delta C_d = (1 - g - (\sqrt{1 - p} - \sqrt{g - p})(2 - \sqrt{1 - p} - \sqrt{g - p})) \frac{\Delta A}{S_{for}} \quad (3.10)$$

where

$$g = \frac{(P_t - P_{c_2}) - (P_s - P_{c_2})_\infty}{(P_t - P_{c_2})_\infty - (P_s - P_{c_2})_\infty} \quad \text{and} \quad p = \frac{(P_s - P_{c_2}) - (P_s - P_{c_2})_\infty}{(P_t - P_{c_2})_\infty - (P_s - P_{c_2})_\infty},$$

and ΔA is the panel area and S_{for} is the three-dimensional radome surface area. Note that all aerodynamic forces and moments are normalized by S_{for} in this thesis.

The total drag coefficient, C_d , was determined by summing the incremental drag coefficients of each panel over all the panels:

$$C_d = \sum_{i=1}^N \Delta C_d \quad (3.11)$$

where N is the number of panels in the measurement plane.

The skin friction drag was determined by the identical method, only with the radomes removed from the wind-tunnel. The skin friction drag was then subtracted from the total drag (measured with the radomes installed), to get the drag increment due to the radome installation on the aircraft.

3.5.2 Lift, Pressure Drag and Pitching Moment Determination

The changes in lift, pressure drag and pitching moment on the aircraft due to the presence of the radomes were also of interest in this study.

As mentioned in Section 3.1.1 (*Radome Design*), the radomes were fitted with static pressure taps on one side of their plane of symmetry. For convenience, the static tap locations corresponded to panel corner points, as defined by the PMARC output file. At each static tap on the model, a static pressure measurement, $(P_s - P_{c_2})$, and a reference dynamic pressure measurement, $(P_{c_1} - P_{c_2})$, were recorded simultaneously. At each ceiling static tap, a freestream static pressure measurement, $(P_\infty - P_{c_2})$, and a reference dynamic pressure measurement, $(P_{c_1} - P_{c_2})$, were also recorded simultaneously. The pressure coefficients on the radomes were thus determined as follows:

$$C_p = \frac{(P_s - P_{c_2})}{P_{dyn_a}} - \frac{(P_\infty - P_{c_2})_{local}}{P_{dyn_b}} \quad (3.12)$$

where subscripts a and b refer to the dynamic pressures (deduced from the $(P_{c_1} - P_{c_2})$ readings) taken simultaneously with the radome static pressure measurements and ceiling static pressure measurements, respectively. Note that the pressure coefficients are referenced to the *local* ceiling static pressure, that is, the ceiling static pressure at the same streamwise position as the radome tap. See Section 3.4.2 for the relationship between $(P_{c_1} - P_{c_2})$ and P_{dyn} .

The PMARC output file also provided the area of each panel and a fully defined unit normal vector at each panel centroid. This information was used in reducing the experimental data. Averaging the pressure coefficients of any four static taps surrounding a particular panel yielded the approximate pressure coefficient at the panel centroid. The components of force coefficient acting on that panel were then determined, knowing the panel area as well as its fully defined unit normal vector. Specifically, the lift and pressure-drag coefficient components were calculated by multiplying the pressure coefficient at the panel centroid by the panel area and the component of the unit normal vector in the directions perpendicular and parallel to the freestream flow, respectively. Note that the results were multiplied by a factor of two to account for the force components acting on the other side of the radome, which were assumed to be the same, given the model symmetry.

In the wind-tunnel experiment, when no radomes were present, it was found that

the static pressure at any given streamwise position along the fairing was essentially the same as that on the tunnel ceiling (which corresponds to the freestream static pressure), at the same streamwise position. This implies that when there are no radomes, the static pressure on the fairing on the aircraft would essentially equal to the freestream static pressure. Consequently, the lift and pressure-drag contributed by each panel on the radome are given by

$$\Delta C_l = C_p \times \Delta A \times \hat{n}_{x-y} \quad (3.13)$$

and

$$\Delta C_{pd} = C_p \times \Delta A \times \hat{n}_{y-z} \quad (3.14)$$

with C_p given by Equation 3.12. The total lift and pressure-drag force coefficients acting on the radomes were then calculated by summing their respective components over all the panels covering the radome halves and multiplying the sum by a factor of two as follows:

$$C_l = 2 \times \sum_{i=1}^N \Delta C_l \quad (3.15)$$

and

$$C_{pd} = 2 \times \sum_{i=1}^N \Delta C_{pd} \quad (3.16)$$

where N is the number of panels covering the radome halves.

The pitching moment coefficient, C_m , was derived similarly. For the aircraft case, it is important to know C_m about the aircraft's centre of gravity, as that will ultimately dictate the required deflection of control surfaces to compensate for the perturbation caused by the radomes. In this experiment, the pitching moment coefficient was determined about the leading edge of the forward-radome. However, it could easily be transferred to the moment about the centre of gravity of the aircraft. To determine the incremental pitching moment coefficient at each panel centroid, the incremental lift coefficient at each panel centroid, ΔC_l , was multiplied

by that centroid's axial distance to the leading edge of the forward-radome, $x_{i/for}$, and divided by radome separation distance, c_{sep} . The change in pitching moment coefficient about the leading edge of the forward-radome on the aircraft due to the radomes was then calculated by summing the components of all the panels covering the radome halves and multiplying the sum by a factor of two as follows:

$$C_m = 2 \times \frac{\sum_{i=1}^N \Delta C_l \times x_{i/for}}{c_{sep}} \quad (3.17)$$

The pitching moment contribution due to incremental drag is negligible.

3.6 Test Conditions

Table 3.3 presents the nominal wind-tunnel test conditions for this study. The Reynolds number is based on the maximum forward-radome width of 6.9 in.

Table 3.3: Nominal Wind-Tunnel Test Conditions

Variable	Value
Ambient Temperature (typical)	25°C
Wind-Tunnel Temperature (typical)	38°C
Wind Speed	65 m/s
Mach Number	0.19
Reynolds Number	6.5×10^5

Chapter 4

Results and Discussion

This chapter begins with some panel method computations, showing trends in C_p distribution over the radomes for various attack and yaw angle configurations. Contour plots of momentum-flux-deficit from wake surveys are then presented and discussed, clearly depicting the nature of the flow downstream of each radome. Flow visualization experiments indicate regions of separated and reverse flow. Measured total drag coefficients obtained from wake surveys are tabulated. Then, contour plots of measured C_p values over the radomes are shown, and measured lift, pressure-drag and pitching moments are tabulated. An uncertainty analysis of the measured aerodynamic coefficients is then performed. All aerodynamic coefficients are then corrected for compressibility effects, and the corrected total drag, lift, pressure-drag and pitching moments are tabulated. Finally, application to full-scale aircraft is described.

4.1 Panel Method Computations

Panel method computations were carried out to examine inviscid flow trends for various speed and attitude configurations. Figure 4.1 illustrates the computational pressure coefficient distribution over the forward- and aft-radome centrelines, for $M = 0.8, 0.75, 0.70, 0.61, 0.43, 0.26$, and for incompressible flow. The minimum pressure coefficient occurs at $0.38 \times X/L_{for}$ of the forward-radome and at $0.56 \times X/L_{aft}$ of the aft-radome. Note that the panel method computations are based

on incompressible flow theory and compressibility effects are only approximately accounted for, by using the Prandtl-Glauert correction (Saad, 1993). The minimum pressure coefficient at $M = 0.8$ is 67% lower than that for incompressible flow. Note the full pressure recovery predicted by inviscid theory, aft of the minimum pressure positions, on both radomes. As will be seen in Section 4.2.3 (*Flow Visualization*), full pressure recovery does not occur as the flow separates over the aft parts of the radomes.

Table 4.1 lists the computational lift and minimum pressure coefficients for the aforementioned flow speeds. Note that the lift coefficients are quoted as negative, as the perturbation caused by the radomes on the aircraft is a force which decreases the overall aircraft lift.

Table 4.1: Computational Lift and Minimum Pressure Coefficients for Various Mach Numbers

Mach Number	Forward-Radome		Aft-radome	
	C_l	C_{pmin}	C_l	C_{pmin}
0.80	-0.1055	-0.9531	-0.1301	-0.9951
0.75	-0.0957	-0.8645	-0.1180	-0.9027
0.70	-0.0880	-0.7947	-0.1085	-0.8298
0.61	-0.0797	-0.7200	-0.0983	-0.7518
0.43	-0.0703	-0.6347	-0.0867	-0.6628
0.26	-0.0656	-0.5923	-0.0809	-0.6184
Incomp.	-0.0633	-0.5718	-0.0781	-0.5971

Panel method computations were also performed to investigate inviscid trends for different incident freestream flow angles. Figures 4.2 and 4.3 show the pressure coefficient contours at $M = 0.75$ for attack angle, $\alpha = 10^\circ$, and yaw angle, $\beta = 10^\circ$, respectively. Figure 4.4 shows a plot of the pressure coefficient distribution over the radome-centrelines, comparing freestream flow at zero incidence with freestream flow at $\alpha = 10^\circ$ and $\beta = 10^\circ$.

a) Forward Radome

b) Aft Radome

Figure 4.1: Computational C_p Distribution over Radome-Centrelines for Various Mach Numbers

Figure 4.3: Computational C_p Contours over Radomes; $M = 0.75$, $\alpha = 0^\circ$, $\beta = 10^\circ$

a) Forward Radome

b) Aft Radome

Figure 4.4: Computational C_p Distribution over Radome-Centrelines for Various Attitude Configurations; $M = 0.75$

In Figure 4.4, note how the radomes in yawed configuration produce almost identical centreline pressure distributions to those at zero freestream incidence. With the radomes at angle of attack, however, the centreline pressures are higher than those at zero freestream incidence, especially for the forward-radome. This is expected, since at angle of attack, a larger frontal radome area is exposed to the incoming flow at a steeper incident angle. The minimum pressure peaks further downstream on the forward-radome as well.

4.2 Wake Surveys, Flow Visualization and Total Drag

4.2.1 Grid Checks

To measure accurate drag coefficients from wake surveys, a sufficient number of measurements in the survey plane must be recorded, especially in regions of large pressure gradients, such as near the floor and in the wake. To justify choice of grid density, three different grid arrangements were tested and compared, using the same survey test data. Momentum-flux-deficit contours and computed drag coefficients obtained from the three grid arrangements are shown in Figure 4.5. The numerical values associated with all such momentum-flux-deficit contour plots in this thesis are determined from the ratio of the incremental drag coefficient of each panel, ΔC_d , to their respective panel area, ΔA . The Z -label in Figure 4.5 and for all such figures hereafter refers to the vertical distance from the base of the model in the plane of measurement. The Y -label refers to the span, where $Y = 0$ marks the test-section centreline.

Figure 4.5 (c), the most dense grid, illustrates the smoothest and most accurate momentum-flux-deficit contours. Compared to the grid of Figure 4.5 (b), twice as many pressure measurements are taken between $Z = 0$ and $Z = 1.25$ in., as the vertical step size is halved in that region. However, a change in drag coefficient of only 1.1% between the grids in Figures 4.5 (b) and (c), indicates that the density of the grid in Figure 4.5 (c) is more than adequate. The grid arrangement of Figure

a) coarse grid; $C_d = 0.0380$ b) medium grid; $C_d = 0.0363$ c) fine grid; $C_d = 0.0359$

Figure 4.5: Wake Surveys for Three Grid Densities; Momentum-Flux-Deficit Contours ($\Delta C_d/\Delta A$)

4.5 (c) was thus chosen in the reduction of measured wake survey drag and the presentation of all momentum-flux-deficit contour plots in this thesis.

4.2.2 Wake Surveys

Momentum-Flux-Deficit Contours at Design and $1.1\times$ Design Radome Separation

Figure 4.6 illustrates momentum-flux-deficit contours in measurement plane II at design and $1.1\times$ design radome separation. Figure 4.7 shows the static pressure variation in measurement plane II at five span positions for both radome separations. The measured static pressures are referenced to P_{c_2} , and normalized by freestream dynamic pressure in the measurement plane.

As mentioned in Section 3.5.1 (*Total Drag Determination*), two adjacent regions were traversed in each measurement plane. The *centreplane* region contains the wake core. It extends from $Y = 4.13$ in. to $Y = -3.88$ in., as seen in Figure 4.6. The *adjacent* region extends from $Y = -3.88$ in. to $Y = -12$ in. to capture the flow field closer to the near wall (at $Y = -15$ in.). The contour boundaries agree very well where the two planes meet at -3.88 in. Rather than survey a third plane to capture the flow field closer to the far wall, a *mirror image* of the *adjacent* plane was positioned at $Y = 4.13$ in. This explains the slightly jagged contour lines at $Y = 4.13$ in., where the *mirror image plane* and *centreplane* meet.

The surveys show similar momentum-flux-deficit contours for both cases. The wakes are symmetrical about the wind-tunnel centreline, and the upward bulges in the contours beside the central wake clearly indicate the presence of horseshoe vortices at the base of the radomes. Such horseshoe vortices are usually present at the base of protuberances in a boundary-layer flow and are thus to be expected in the present flow. The flow visualization results presented later, in Section 4.2.3, support the existence of horseshoe vortices. Note the slight asymmetry in the momentum-flux-deficit contours at 3 in. left and 3 in. right of the wind-tunnel centreline. It appears the clockwise leg of the vortex on the left side of the radomes (looking downstream) convects freestream fluid closer to the floor than the counterclockwise

a) design radome separation; $C_d = 0.0929$ b) 1.1 x design radome separation; $C_d = 0.0903$

Figure 4.6: Momentum-Flux-Deficit Contours in Measurement Plane II at Design and 1.1x Design Radome Separation

a) design radome separation

b) 1.1 x design radome separation

Figure 4.7: Static Pressure Variation in Measurement Plane II at Design and 1.1x Design Radome Separation

leg on the right side of the radomes. This asymmetry is somewhat more amplified at $1.1\times$ design radome separation than design radome separation.

The wake diffuses to a greater area at design radome separation, as seen in Figure 4.6. This occurs because the measurement plane is further downstream of the aft-radome for the design radome separation case than for the $1.1\times$ design radome separation case. An approximate wake diffusion angle relative to the mean flow direction of 19° was calculated. This angle was determined by measuring the difference in heights at which C_d fell to zero in the measurement plane, and knowing the distance between the aft-radome and the measurement plane, for both radome separations.

In Figure 4.7, the shapes of the static pressure distributions for both cases are similar. The centreline distribution has a negative peak at approximately 1.2 in. ($0.37 \times H_{aft}$) from the base plate, for both radome separations. This location corresponds to the maximum momentum-flux-deficit, as seen in Figure 4.6. The static pressure distributions at equal distances from the wind-tunnel centreline are slightly different, and can be attributed to a difference in horseshoe vortex characteristics at these locations. At 12 in. left of centreline, the static pressure distributions are essentially uniform (i. e. freestream conditions are achieved). Thus, the panel method computation of minimal wall interference is justified experimentally (see Section 3.2 (*Preliminary Panel Method Computations*)).

Momentum-Flux-Deficit Contours at Design and $0.8\times$ Design Radome Separation

At $0.8\times$ design radome separation, a bi-stable wake configuration was observed behind the aft-radome. This was somewhat surprising, given the symmetry of the model and the symmetric flow field at design and $1.1\times$ design radome separation. The wake direction was observed by taping tufts of thread to the base plate downstream of the aft-radome. The tufts fluttered in the direction of the wake, which periodically shifted about the wind-tunnel axis. The shift in the wake direction was a few degrees. It is presumed that a slight asymmetric disturbance in the flow field

caused by the forward-radome is amplified by the close proximity of the aft-radome.

Figures 4.8 and 4.9 show the momentum-flux-deficit contours in the centreplane of measurement planes I and II, respectively, for both the design and $0.8\times$ design radome separations. In Figure 4.8, a slight asymmetry in the wake is observed at both radome separations, but the asymmetry is more pronounced for the $0.8\times$ design radome separation case. The momentum-flux-deficit contours peak 0.15 in. left of centreline, and the wake is broader to the right of centreline. With $0.8\times$ design radome separation, one can speculate that the effects upstream of the aft-radome propagate upstream to measurement plane I. This is due to the shorter distance between the aft-radome's leading edge and measurement plane I for $0.8\times$ design radome separation ($0.58 \times L_{for}$ for $0.8\times$ design radome separation versus $0.94 \times L_{for}$ for design radome separation). Note how the peaks of the momentum-flux-deficit contours shift from about 0.6 in. right of centreline (for $Z = 0$ to 0.6 in.) to 0.15 in. left of centreline above $Z \approx 0.6$ in.

In Figure 4.9 (b) ($0.8\times$ design radome separation), the wake centre (location of maximum momentum deficit) is offset 0.6 in. to the left of the wind-tunnel axis. Effects of the horseshoe vortices are less apparent, but this is presumably due to the longer distance between the aft-radome and measurement plane II at $0.8\times$ design radome separation. Bearing in mind that the contours presented are obtained from time-averaged wake surveys, it can be concluded from Figure 4.9 (b) that the dominant wake direction is angled to the left of the wind-tunnel axis. Taking the mean direction of the wake as that of the wake centre, then from the offset of 0.6 in. and the position of measurement plane II relative to the separation point along the aft-radome-centreline, (see Section 4.2.3 (*Flow Visualization*)), the approximate angular direction of the wake is 1.9° to the left of the wind-tunnel axis.

A momentum-flux-deficit coefficient of the wake core, $C_{d_{core}}$, was calculated from the wake surveys of Figures 4.8 and 4.9. $C_{d_{core}}$ values in measurement plane I at design and $0.8\times$ design radome separation differ by only 1.7%. Those in measurement plane II differ by only 3.1%. Note that the wake core boundaries are somewhat arbitrarily chosen, and the $C_{d_{core}}$ values computed are rough estimates at best. They do, however, indicate that the drag of the radomes is essentially the same for ei-

a) design radome separation; $C_{dcore} = 0.0118$

b) 0.8 x design radome separation; $C_{dcore} = 0.0120$

Figure 4.8: Momentum-Flux-Deficit Contours in Wake Core of Measurement Plane I at Design and 0.8× Design Radome Separation

a) design radome separation; $C_{dcore} = 0.0359$

b) 0.8 x design radome separation; $C_{dcore} = 0.0348$

Figure 4.9: Momentum-Flux-Deficit Contours in Wake Core of Measurement Plane II at Design and 0.8× Design Radome Separation

ther of the two wake configurations observed for the $0.8\times$ design radome separation distance.

Forward-Radome Drag

The drag due to the forward-radome was also of interest in this study. Results of a wake survey in measurement plane I are presented in Figure 4.10, illustrating the momentum-flux-deficit contours and static pressure variation. For clarity purposes, the Z -axis has been magnified.

Figure 4.10 (a) shows that the wake core downstream of the forward-radome is narrower than the wake core downstream of the aft-radome (see Figure 4.6 (a)); this is to be expected. The momentum deficit in the wake core decreases monotonically from the base of the model until the freestream is reached. The effects of the horseshoe vortices are clearly evident. The effects of the wake and horseshoe vortex pair are evident over a span of about 18 in. (from $Y = -9$ in. to $Y = 9$ in.)

Figure 4.10 (b) shows the static pressure variation in measurement plane I as a function of height from the model base. The model base in measurement plane I is the fairing surface. Data are plotted for five span positions. The apparent increase in static pressure very close to the fairing surface at all span positions is attributed to interference effects between the fairing surface and the static tube of the Pitot/static probe assembly (Pankhurst and Holder, 1965).

a) Cd Contours; $C_d = 0.0420$

b) Static Pressure Variation

Figure 4.10: Momentum-Flux-Deficit Contours and Static Pressure Variation in Measurement Plane I; Design Radome Separation

Reynolds Number Effects

A wake survey in the centreplane of measurement plane II at a higher Reynolds number (7.79×10^5) was performed to validate the results at standard Reynolds number (6.51×10^5) at which the majority of the experiments were carried out. The Reynolds number test was performed at design radome separation. Figure 4.11 displays the momentum-flux-deficit contours for the standard Reynolds number and high Reynolds number test cases. The Reynolds numbers are based on the forward-radome width (6.9 in.).

The results obtained from the higher Reynolds number test were inconclusive, most probably due to errors in static pressure measurements. Lack of time did not permit a repeat of the test. The static pressure variation with vertical height changed significantly at $Z \approx 2$ in., which could explain the uncharacteristic ‘kinks’ in the momentum-flux-deficit contours of Figure 4.11 (b).

It should be recalled that the approach-flow was turbulent, so Reynolds number effects would be expected to be small. In any case, the expected accuracy (see Section 4.4) of the present measurements is insufficient to enable a detection of mean Reynolds number effects for a Reynolds number change of only 20%.

Skin Friction Drag

Recall from Section 3.5.1 (*Total Drag Determination*), that skin friction drag of the basic fuselage surface is subtracted from wake survey drag (i.e. with the radomes installed) to isolate the drag of the radomes on the aircraft. Figures 4.12 and 4.13 illustrate the momentum-flux-deficit contours and static pressure distribution over the basic fuselage surface (i.e. with the radomes removed) in measurement planes I and II, respectively. Skin friction drag computed from these surveys is subtracted from the wake survey results shown earlier, giving the net C_d 's presented later, in Section 4.2.4 (*Total Drag*).

a) $Re = 6.51E05$ (standard); $Cd_{core} = 0.0359$

b) $Re = 7.79E05$ (high); $Cd_{core} = 0.0325$

Figure 4.11: Momentum-Flux-Deficit Contours at Different Reynolds Numbers

a) Cd Contours; $C_d = 0.0418$

b) Static Pressure Variation

Figure 4.12: Momentum-Flux-Deficit Contours and Static Pressure Variation in Measurement Plane I, Without Radomes

a) Cd Contours; $C_d = 0.0650$

b) Static Pressure Variation

Figure 4.13: Momentum-Flux-Deficit Contours and Static Pressure Variation in Measurement Plane II, Without Radomes

4.2.3 Flow Visualization

Flow visualization experiments were performed to locate regions of separated and reverse flow, identify horseshoe vortex pairs around radome bases, and observe shear stress patterns. A mixture containing white titanium dioxide powder and teak furniture oil was applied to the radomes, fairing skin and base plate. The radomes were at design separation. Experiments were carried out at a wind-speed of about 65 m/s for 4-5 minutes. Thin tape was used to cover fairing and base plate screws and fairing static taps, ensuring a smooth surface. A single stretch of thin tape was placed along the fairing skin/base plate joints, on either side of the radomes, so as to prevent a blunt fairing skin edge. Radome static taps remained uncovered, as tape did not adhere well to the radome surface. The results did not suffer from uncovered radome static taps, and tape edge effects were minimal.

Figure 4.14 shows the front face of the forward-radome. Some interesting features are observed here which are comparable to those presented by Baker (1980), Pierce and Harsh (1988), Pierce and Tree (1990), and Bain and Fletcher (1993), who examined the turbulent horseshoe-vortex system around the base of wall-mounted bluff bodies (see Chapter 2). In particular, Pierce and Tree's 2-vortex pattern consisting of one dominant or primary junction vortex and one small, tightly-wound body/floor corner vortex are observed in the present results.

Approximately 3 in. upstream of the forward-radome leading edge, along the wind-tunnel centreline, a saddle point of primary separation can be seen. Two rays of ordinary separation points emerge from the primary saddle point, forming a primary separation line which envelopes the radome base. Flow separating at the primary saddle point in the radome plane of symmetry rolls up into the dominant or primary vortex, and trails downstream on either side of the radome. At approximately 1 in. upstream of the forward-radome leading edge, a distinct line enveloping the radome base is seen. Previous researchers (Steinheuer (1965), Belik (1973), Baker (1979, 1980)), reported that this distinct line corresponds to the projection of the vortex centre on the floor.

The band between the distinct line and the radome base, devoid of oil mixture,

is indicative of high shear stresses caused by the primary vortex. The shear stress pattern in this band clearly shows the effect of the primary vortex in convecting the oil mixture outboard of the radome. Note that the primary vortex grows as it trails downstream. The primary vortex separation line and the distinct line converge at the fairing skin/base plate joint.

The white band of oil mixture accumulated at the extreme corner formed by the radome and the floor is caused by corner separation of the primary vortex. The corner vortex thus produced trails downstream along the radome base on either side, and is of opposite sign to the primary vortex. This is consistent with the theory that vortices often occur in counter-rotating pairs.

The primary vortex continues downstream, as seen in Figure 4.15, which shows the back face of the forward-radome (i.e. looking upstream). Accumulation of a line of oil mixture on the back face of the forward-radome is evidence of a separation line. The flow separates at $X/L_{for} = 0.86$ on the radome-centreline, which, as will be seen in Section 4.3, is a visual confirmation of the measured centreline pressure distribution (see Figure 4.20 (a)). The separation line is clearly three-dimensional. The flow remains attached longer over the sides of the radome, due to its streamlined shape. The patches of oil mixture aft of the separation line and on the radome are due to dried oil mixture from previous flow visualization experiments, and should be ignored. The shear stress pattern in this region is not clearly depicted in Figure 4.15, although it should be noted that reverse flow occurred, as evidenced by oil flow patterns observed during the wind-tunnel experiment.

Also seen in Figure 4.15 are two strips of accumulated oil mixture trailing downstream from either end of the separation line at the base of the radome, converging to a narrow tail centered about the wind-tunnel axis. The two strips are separation lines caused by the trailing corner vortex produced at the radome's leading edge.

Figures 4.16 and 4.17 show the surface flow pattern around the front and side of the aft-radome. Flow characteristics are similar to those observed for the forward-radome. In Figure 4.17, a primary saddle point along the wind-tunnel centreline is seen 2 in. upstream of the leading edge of the aft-radome (compared to 3 in. for the forward-radome). The distinct line corresponding to the projection of the vortex

centre intersects the wind-tunnel centreline at approximately 0.25 in. upstream of the leading edge of the aft-radome. The aft-radome primary vortex is thus smaller, presumably due to the less-steeply-sloped radome leading edge. No corner separation of the primary vortex is observed for the aft-radome, although it may be too small to detect from the present results.

Figure 4.18 shows the back face of the aft-radome. The primary vortex separation line and the distinct line merge at the fairing skin/base plate joint and continue downstream on either side of the radome. This is more apparent on the right side of Figure 4.18 than the left. The deposit of titanium dioxide in the wake of the radome and the symmetric surface streamline pattern in the wake are products of unsteady wake flow (Baker, 1980). The separation line on the aft-radome is also visible. The flow separates at $X/L_{for} = 0.84$ on the aft-radome-centreline, which, as will be seen in Section 4.3, is a visual confirmation of the measured centreline pressure distribution (see Figure 4.20 (b)). The separation line spans the width of the radome and is essentially two-dimensional (i.e. the flow separates at the same approximate streamwise station of the radome at all points). A portion of the lower back face of the aft-radome is covered with oil mixture. This is caused by spraying action at the base of the aft-radome, presumably due to unsteady wake effects.

Note that the formation of a tail trailing in the wake is not apparent. This is consistent with the absence of a detectable body/floor corner vortex at the aft-radome leading edge.

Figure 4.14: Oil-Film Flow Visualization over Forward-Radome; Front Face

Figure 4.15: Oil-Film Flow Visualization over Forward-Radome; Back Face

Figure 4.16: Oil-Film Flow Visualization over Aft-Radome; Front Face

Figure 4.17: Oil-Film Flow Visualization over Aft-Radome; Side Face

Figure 4.18: Oil-Film Flow Visualization over Aft-Radome; Back Face

4.2.4 Total Drag

Complete wake surveys in measurement plane II were carried out at design and $1.1\times$ design radome separation, for total drag determination (see Section 4.2.2 (*Wake Surveys*)). A complete wake survey in measurement plane I was carried out at design radome separation, to determine the drag of the forward-radome. At $0.8\times$ design radome separation, difficulties arose in surveying both the centreplane *and* adjacent plane, given the bi-stable wake condition downstream of the aft radome. Thus, a comparison between the $0.8\times$ design radome separation and design radome separation cases is made based on the wake core momentum-flux-deficit coefficient, $C_{d_{core}}$. The total drag coefficients presented in this section are obtained by subtracting measured skin friction drag (i.e. with the radomes removed) from measured wake survey drag (i.e. with the radomes installed).

Table 4.2 shows the measured total drag coefficients at design and $1.1\times$ design radome separation.

Table 4.2: Measured Total Drag Coefficient

Radome	Radome Separation	
	Design	$1.1\times$ Design
Forward	0.0033	-
Both	0.0273	0.0247

From Table 4.2, note that the total drag coefficient of both radomes is slightly higher at design radome separation than at $1.1\times$ design radome separation (by 9.4%). An explanation for this higher drag measurement is as follows. At $1.1\times$ design separation, the fairing skin and aft-radome intersect farther downstream, where the fairing height above the tunnel floor is less. A longer stretch of downward sloping fairing before the aft-radome implies a greater freestream static-pressure rise and a thicker boundary-layer, or perhaps even some separation in the flow approaching the radome. The aft-radome would thus be more *shielded*, resulting in lower pressure on its front face, and therefore, lower total drag increment.

Table 4.3 shows the measured wake core momentum-flux-deficit coefficients at design and $0.8 \times$ design radome separation.

Table 4.3: Measured Wake Core Momentum-Flux-Deficit Coefficient

Radome	Radome Separation	
	Design	$0.8 \times$ Design
Forward	-0.0011	-0.0009
Both	0.0140	0.0129

Since the core momentum-flux-deficit coefficients are nearly the same, it can be deduced that the total drag coefficient with $0.8 \times$ design radome separation distance is approximately the same as that for the design radome separation distance, that is 0.027.

4.3 Measured Lift, Pressure-Drag and Pitching Moment

Measured lift, pressure-drag and pitching moment coefficients, for the three radome separation distances, are presented in this section.

Figure 4.19 illustrates measured pressure coefficient contours over the radomes. Figure 4.20 shows the radome-centrelines pressure coefficient distribution. The measured lift, pressure-drag and pitching moment coefficients are shown in Table 4.4. Recall that all aerodynamic coefficients are normalized by the forward-radome surface area, S_{for} . Pitching moment coefficients are also normalized by radome separation distance, c_{sep} , and the pitching moment is about an axis through the leading edge of the forward-radome. Recall too that the values represent force and moment *increments* due to the presence of the radomes.

From Figures 4.19 and 4.20, the forward-radome is seen to have very similar pressure distributions at all radome separation distances, as expected. This is also reflected in the measured forward-radome lift coefficients. Flow separation clearly

Figure 4.19: Measured Pressure Coefficient Contours and Lift Coefficients over Radomes

a) Forward Radome

b) Aft Radome

Figure 4.20: Measured Radome-Centreline Pressure Coefficient Distribution

Table 4.4: Measured Lift, Pressure-Drag and Pitching Moment Coefficients

Radome Sep.	Forward-Radome			Aft-Radome			Both Radomes		
	C_l	C_{pd}	C_m	C_l	C_{pd}	C_m	C_l	C_{pd}	C_m
Des.	-0.1226	6.19E-07	0.0285	-0.0973	0.0059	0.1756	-0.2199	0.0059	0.2041
1.1× Des.	-0.1181	3.42E-05	0.0249	-0.1044	0.0134	0.1826	-0.2224	0.0134	0.2075
0.8× Des.	-0.1216	7.53E-04	0.0349	-0.1130	0.0063	0.2233	-0.2347	0.0070	0.2582

occurs over the aft-radome, and the separation line is seen to move upstream with decreasing radome separation. This is attributed to the fact that the wake impinging flow on the aft-radome is less diffuse for smaller radome separation distances. Flow separation from the forward-radome is less evident from Figure 4.19, although the flow clearly does separate from the back face of the forward-radome, as seen by the separation line in the flow visualization photo, Figure 4.15.

From Table 4.4, the design configuration is seen to produce the best aerodynamic coefficients. The smallest decrease in overall lift, and smallest increases in pressure-drag and pitching moment are observed for the design radome separation distance.

The pressure-drag is very small on the forward-radome. It is presumed that freestream fluid entrained by the trailing horseshoe vortices is impressed upon the aft face of the forward-radome. The resulting higher pressures over the back face of the radome produce a balancing force against the pressures on the front face of the radome, causing very little net pressure-drag. This effect is less observed for the aft-radome, due to a larger region of separated flow and consequent lower pressures over its back face, clearly evident in Figure 4.19. As expected, the pressure-drag coefficients are substantially less than the total-drag coefficients presented Table 4.2, because the latter include skin-friction drag.

4.4 Uncertainty Analysis

The measured aerodynamic coefficients in this research are clearly subject to uncertainty, which is attributed to a variety of sources.

The SETRA transducer calibration is one source of error. Calibration uncertainty stems from U-tube manometer readings, which cannot be resolved to an accuracy better than 0.25 mm, and the Digital Fluke Multimeter 8080A readout. This is evident from the scatter in the data through which a least-squares ‘best-fit’ straight line is passed in estimating the transducer calibration coefficient (see Figure 3.11 (*Calibration of Setra Pressure Transducers*)).

The data acquisition unit is another source of error. As explained in Section 3.3.2 (*Phase I: Wake Survey*), the weight of the least significant bit of the A/D digital converter of the data acquisition unit is 1.22 mV. This represents an uncertainty of 0.024% in the data acquisition unit’s full-scale differential output of ± 5 V.

Kline and McClintock have presented a means of estimating the uncertainty in a calculated result on the basis of the uncertainties in the primary measurements which, together, form the calculated result (Holman, 1984). Kline and McClintock estimate uncertainty as follows:

$$w_R = \left(\left(\frac{\partial R}{\partial x_1} w_1 \right)^2 + \left(\frac{\partial R}{\partial x_2} w_2 \right)^2 + \cdots + \left(\frac{\partial R}{\partial x_n} w_n \right)^2 \right)^{\frac{1}{2}}, \quad (4.1)$$

where w_R is the uncertainty in a given result, R , and $w_1, w_2, \dots, w_n$ are the uncertainties in the independent (measured) variables $x_1, x_2, \dots, x_n$. The uncertainties in all independent variables and the result, R , are assumed to have the same odds.

It was found that the uncertainty in the calibration of the SETRA pressure transducers due to the abovementioned errors was significantly larger than the uncertainties in the data acquisition instrumentation. It has been shown that since the uncertainty propagation in the result, w_R , predicted by Equation 4.1, depends upon the square of the uncertainties, the independent variables having uncertainties 5 times less than others have a notably minimal effect on w_R (Holman, 1984). The very small degree of uncertainty in the data acquisition unit was thus not taken into account in determining the uncertainty in the measured aerodynamic coefficients.

Methods for determining uncertainties in the aerodynamic coefficients for both phases of the experiment are presented in this section.

4.4.1 Uncertainty in Measured Total Drag

Applying Equation 4.1 to Betz's formula (Equation 3.9) for panel drag coefficient, ΔC_d , the uncertainty in panel drag coefficient, $w_{\Delta C_d}$, becomes:

$$w_{\Delta C_d} = \left(\left(\frac{\partial(\Delta C_d)}{\partial(P_t - P_{c2})} w_{setra1} \right)^2 + \left(\frac{\partial(\Delta C_d)}{\partial(P_s - P_{c2})} w_{setra1} \right)^2 + \right. \quad (4.2)$$

$$\left. + \left(\frac{\partial(\Delta C_d)}{\partial(P_t - P_{c2})_\infty} w_{setra1} \right)^2 + \left(\frac{\partial(\Delta C_d)}{\partial(\Delta A)} w_{\Delta A} \right)^2 \right)^{\frac{1}{2}},$$

where all partial derivatives are taken with respect to the independent variables in Betz's formula ($(P_t - P_{c2})$, $(P_s - P_{c2})$, $(P_t - P_{c2})_\infty$ and ΔA). The first three independent variables are derived from measurements taken by SETRA pressure transducer 1, and are therefore subject to the same uncertainty. The range of pressures over which the transducer was calibrated was 0 - 3250 Pa. The uncertainty in pressure measurements from the SETRA transducer 1 calibration curve, w_{setra1} , was determined within a 95% confidence interval as ± 19 Pa (see Figure 3.11). This represents the uncertainty associated with the resolution capabilities of the transducers and does not incorporate any bias error. Bias error was eliminated by subtracting the zero-pressure voltage reading at the time of the measurements. In percentage terms of full scale pressure range, the uncertainty in pressure measurements becomes 1.14%. Note that this is far greater than the error introduced by the data acquisition unit, which represents an uncertainty of 0.024% of full scale differential output ± 5 V. Uncertainty in panel area stems from Pitot/static probe traverse distances, which are assumed to be accurate to within ± 0.25 mm. This value represents the accuracy to which the measurements could be resolved with the ruler used. A 95% confidence interval is assumed for the uncertainty in panel area. This permits an uncertainty determination in ΔC_d , with the same 95% confidence interval.

The total uncertainty in drag coefficient, w_{c_d} , is the sum of the uncertainties of all radome panels:

$$w_{C_d} = \sum_{i=1}^N w_{\Delta C_d} \quad (4.3)$$

where N represents the number of radome panels in the measurement plane. Total drag coefficients and their respective uncertainties are presented in Section 4.5, Table 4.5, where all aerodynamic coefficients are corrected for compressibility effects.

4.4.2 Uncertainty in Measured Lift, Pressure-Drag and Pitching Moment

Applying Equation 4.1 to the formula derived in Section 3.5.2 for panel lift coefficient, ΔC_l , (see Equation 3.13), the uncertainty in panel lift coefficient, $w_{\Delta C_l}$, becomes:

$$w_{\Delta C_l} = \left(\left(\frac{\partial(\Delta C_l)}{\partial(P_s - P_{c_2})} w_{setra1} \right)^2 + \left(\frac{\partial(\Delta C_l)}{\partial(P_\infty - P_{c_2})} w_{setra1} \right)^2 + \right. \quad (4.4) \\ \left. + \left(\frac{\partial(\Delta C_l)}{\partial(P_{dyn_a})} w_{setra2} \right)^2 + \left(\frac{\partial(\Delta C_l)}{\partial(P_{dyn_b})} w_{setra2} \right)^2 + \left(\frac{\partial(\Delta C_l)}{\partial(\Delta A)} w_{\Delta A} \right)^2 \right)^{\frac{1}{2}}$$

where w_{setra1} and w_{setra2} are the uncertainties in the pressure measurements obtained from the SETRA transducer calibration curves (see Figure 3.11), and P_{dyn_a} and P_{dyn_b} are the dynamic pressures recorded simultaneously with static pressure measurements on the radomes and the ceiling, respectively. The total uncertainty in lift coefficient, w_{C_l} , is the sum of the uncertainties of all radome panels:

$$w_{C_l} = \sum_{i=1}^N w_{\Delta C_l} \quad (4.5)$$

where N represents the number of radome panels. Similar expressions are derived for the uncertainty in pressure-drag, $w_{C_{pd}}$, and the uncertainty in pitching moment, w_{C_m} , based on the formulae in Section 3.5.2 for pressure-drag (Equation 3.16) and pitching moment (Equation 3.17).

Lift, pressure-drag and pitching moment coefficients and their respective uncertainties are presented in Section 4.5, Tables 4.6 to 4.8, where all aerodynamic coefficients are corrected for compressibility effects.

4.5 Correction of Measured Aerodynamic Coefficients for Compressibility

Assuming that the flow perturbations caused by the radomes are small and that the flow remains sub-critical over the radomes, linearized theory can be used to correct the measured incompressible aerodynamic coefficients for compressibility effects. The first Prandtl-Glauert similarity rule permits a comparison of pressure, force and moment coefficients for two identical bodies; one immersed in an incompressible flow and the other immersed in a compressible flow (Saad, 1993). The first Prandtl-Glauert rule applied to the pressure, force and moment coefficients, $C_{p/f/m}$, of interest in this study is:

$$C_{p/f/m_{comp}} = \frac{C_{p/f/m_{incomp}}}{\sqrt{1 - M_{\infty}^2}} \quad (4.6)$$

Table 4.5 presents the measured total drag coefficients and Tables 4.6, 4.7 and 4.8 present the measured lift, pressure-drag and pitching moment coefficients, for incompressible flow and for Mach numbers 0.5, 0.6, 0.7, 0.75 and 0.80. All aerodynamic coefficients are corrected for compressibility effects.

Table 4.5: Compressibility-Corrected Measured Total Drag Coefficients of Forward-Radome and Both Radomes

Mach No.	Design Radome Separation		1.1 × Design Radome Separation
	Forward-Radome	Both Radomes	Both Radomes
Incomp.	0.0033 ± 0.0003	0.0273 ± 0.0073	0.0247 ± 0.0078
0.50	0.0038	0.0315	0.0285
0.60	0.0041	0.0341	0.0309
0.70	0.0046	0.0382	0.0346
0.75	0.0050	0.0413	0.0373
0.80	0.0055	0.0455	0.0412

Table 4.6: Compressibility-Corrected Measured Lift, Pressure-Drag and Pitching Moment Coefficients; Design Radome Separation

Radome	Mach No.	$C_{l_{comp}}$	$C_{pd_{comp}}$	$C_{m_{comp}}$
Forward	Incomp.	-0.1226 ± 0.0067	$6.19\text{E-}07 \pm 0.0027$	0.0285 ± 0.0010
	0.50	-0.1415	7.15E-07	0.0329
	0.60	-0.1532	7.74E-07	0.0356
	0.70	-0.1716	8.67E-07	0.0398
	0.75	-0.1853	9.36E-07	0.0430
	0.80	-0.2043	1.03E-06	0.0474
Aft	Incomp.	-0.0973 ± 0.0055	0.0059 ± 0.0024	0.1756 ± 0.0057
	0.50	-0.1124	0.0069	0.2028
	0.60	-0.1217	0.0074	0.2195
	0.70	-0.1363	0.0083	0.2459
	0.75	-0.1471	0.0090	0.2655
	0.80	-0.1622	0.0099	0.2927
Both	Incomp.	-0.2199 ± 0.0121	0.0059 ± 0.0051	0.2041 ± 0.0067
	0.50	-0.2539	0.0069	0.2356
	0.60	-0.2749	0.0074	0.2551
	0.70	-0.3079	0.0083	0.2858
	0.75	-0.3324	0.0090	0.3085
	0.80	-0.3665	0.0099	0.3401

Table 4.7: Compressibility-Corrected Measured Lift, Pressure-Drag and Pitching Moment Coefficients; $1.1\times$ Design Radome Separation

Radome	Mach No.	$C_{l_{comp}}$	$C_{pd_{comp}}$	$C_{m_{comp}}$
Forward	Incomp.	-0.1180 ± 0.0067	$3.42\text{E-}05 \pm 0.0027$	0.0249 ± 0.0010
	0.50	-0.1363	3.95E-05	0.0288
	0.60	-0.1475	4.28E-05	0.0311
	0.70	-0.1652	4.79E-05	0.0349
	0.75	-0.1784	5.17E-05	0.0377
	0.80	-0.1967	5.70E-05	0.0415
Aft	Incomp.	-0.1044 ± 0.0056	0.0134 ± 0.0025	0.1826 ± 0.0062
	0.50	-0.1205	0.0155	0.2109
	0.60	-0.1304	0.0168	0.2283
	0.70	-0.1461	0.0188	0.2557
	0.75	-0.1578	0.0203	0.2761
	0.80	-0.1739	0.0224	0.3044
Both	Incomp.	-0.2224 ± 0.0123	0.0134 ± 0.0052	0.2075 ± 0.0072
	0.50	-0.2568	0.0155	0.2396
	0.60	-0.2779	0.0168	0.2594
	0.70	-0.3114	0.0188	0.2906
	0.75	-0.3362	0.0203	0.3137
	0.80	-0.3706	0.0224	0.3459

Table 4.8: Compressibility-Corrected Measured Lift, Pressure-Drag and Pitching Moment Coefficients; 0.8× Design Radome Separation

Radome	Mach No.	$C_{l_{comp}}$	$C_{pd_{comp}}$	$C_{m_{comp}}$
Forward	Incomp.	-0.1216 ± 0.0067	$7.53E-04 \pm 0.0027$	0.0349 ± 0.0010
	0.50	-0.1405	8.69E-04	0.0403
	0.60	-0.1521	9.41E-04	0.0437
	0.70	-0.1703	1.05E-03	0.0489
	0.75	-0.1839	1.14E-03	0.0528
	0.80	-0.2027	1.25E-03	0.0582
Aft	Incomp.	-0.1130 ± 0.0055	0.0063 ± 0.0024	0.2233 ± 0.0051
	0.50	-0.1305	0.0073	0.2578
	0.60	-0.1413	0.0079	0.2791
	0.70	-0.1583	0.0088	0.3127
	0.75	-0.1709	0.0095	0.3376
	0.80	-0.1884	0.0105	0.3722
Both	Incomp.	-0.2347 ± 0.0122	0.0070 ± 0.0051	0.2582 ± 0.0061
	0.50	-0.2710	0.0081	0.2982
	0.60	-0.2933	0.0088	0.3228
	0.70	-0.3286	0.0099	0.3616
	0.75	-0.3548	0.0107	0.3904
	0.80	-0.3911	0.0117	0.4304

4.6 Application to Full-Scale Aircraft

The pressure, force and moment coefficients and their respective uncertainties, presented in Tables 4.5 to 4.8 of the previous section, apply to the full-scale aircraft without further modification. Recall that the lift and drag coefficients are based on freestream dynamic pressure and surface area, S_{for} , of the forward radome: the full-scale value of S_{for} is 3.45 m^2 . The pitching moments are about an axis through the leading edge of the forward radome and the pitching-moment coefficients are based on freestream dynamic pressure, S_{for} , and radome separation distance. Full-scale radome separation distances for the three radome separation distance cases considered are: $c_{sep_{design}} = 4.02 \text{ m}$; $c_{sep_{1.1 \times design}} = 4.42 \text{ m}$; and $c_{sep_{0.8 \times design}} = 3.21 \text{ m}$. Recall too that the wind-tunnel test-section floor boundary-layer was tripped so that the flow over the entire length of the test-section was turbulent. Consequently, the aerodynamic coefficients are not expected to suffer from substantial Reynolds number effects. The shear flow impinging onto the radomes was turbulent (no laminar separation bubbles on the radomes could be inferred from oil-flow visualization experiments) and therefore, the separation lines observed on the aft faces of the model radomes should occur at the same approximate streamwise station on the aircraft radomes.

Chapter 5

Conclusions and Recommendations

5.1 Conclusions

Inviscid panel method computations were performed to investigate trends with aircraft speed and attitude changes. Flow fields were predicted for freestream Mach numbers ranging between $M \approx 0$ (incompressible flow) and $M = 0.80$. Compressibility effects were only approximately accounted for, by using the Prandtl-Glauert correction, which is based on linearized theory. The radome-centreline pressure distributions showed full pressure recovery aft of the minimum pressure positions on both radomes, as expected from inviscid theory. Recall that the panel method used cannot predict flow separation. The predicted minimum pressure coefficient at $M = 0.80$ was 67% lower than that for incompressible flow. Computations were also performed for angle of attack and yaw angle cases at $M = 0.75$. The computed forward-radome centreline pressure coefficients at a 10° angle of attack were generally somewhat higher than at zero-incidence, especially over the top of the radome. The computed forward-radome centreline pressure coefficients at a 10° yaw angle were essentially the same as those at zero-incidence. The computed aft-radome centreline pressure distributions were approximately equal at all incident freestream angles.

Momentum-flux-deficit contours (i.e. plots of $\Delta C_d/\Delta A$) obtained from wake survey data downstream of each radome are presented. Contours downstream of the aft-radome for the design and $1.1\times$ design radome separation distances are similar. They are essentially symmetrical about the wind-tunnel centreline, and the upward bulges in the contours beside the central wake clearly indicated the presence of horseshoe vortices at the base of the radomes. An approximate wake diffusion angle relative to the mean flow direction of 19° was calculated. A uniform vertical static pressure distribution near the test-section walls indicated that panel method computations of minimal wall interference were justified experimentally.

At $0.8\times$ design radome separation distance, a bi-stable wake configuration was observed downstream of the aft-radome. It is presumed that a slight asymmetric disturbance in the flow field caused by the forward-radome was amplified by the close proximity of the aft-radome. The wake's time-averaged angular direction was approximately 1.9° to the left of the wind-tunnel axis. Momentum-flux-deficit coefficients of the wake core for the design and $0.8\times$ design radome separation cases differed by a mere 3.1%, indicating that the drag of the radomes was essentially the same for both configurations.

Results obtained from the higher Reynolds number test were inconclusive, most probably due to errors in static pressure measurements. It should be recalled, however, that the approach-flow was turbulent, so Reynolds number effects are expected to be minimal.

Oil-flow visualization experiments revealed regions of separated and reverse flow, and provided evidence of one dominant primary vortex and one small tightly wound body/floor corner vortex. A primary separation line indicating separation of the primary vortex, and a 'distinct' line presumably corresponding to the projection of the primary vortex centre on the test-section floor, were both observed. The oil-flow patterns were similar to those described in a review of related literature, presented in Chapter 2. Accumulation of lines of oil mixture on the back face of both radomes

clearly indicated flow separation lines. With the radomes at design separation distance, the flow separated at $X/L_{for} = 0.86$ on the forward-radome centreline, and at $X/L_{aft} = 0.84$ on the aft-radome centreline, which was a visual confirmation of the measured radome-centreline pressure distributions.

Measured total drag coefficients for design and $1.1\times$ design radome separation distances are presented in Section 4.2.4 and are tabulated in Table 4.2. The total drag coefficient at design radome separation distance is slightly higher than that at $1.1\times$ design radome separation distance. A comparison between the momentum-flux-deficit coefficients in the wake core of measurement plane II at design and $0.8\times$ design radome separation distance, presented in Table 4.3, show that the total drag of the radomes at both separation distances is approximately the same.

Measured lift, pressure-drag and pitching moment coefficients are presented in Section 4.3 and are tabulated in Table 4.4 for the three radome separation distances. The design configuration was seen to produce the best aerodynamic coefficients; that is, smallest decrease in overall lift, and smallest increases in pressure-drag and pitching moment.

Contour plots of pressure coefficient distribution over the radomes are presented. It was noted that radome separation distance had virtually no effect on the pressure coefficient distribution or lift coefficient of the forward-radome. The contour plots clearly indicated flow separation from the aft-radome, and the flow separation line moved upstream with decreasing radome separation. Pressure-drag values of the forward-radome were very small. It is presumed that the freestream fluid entrained by the trailing horseshoe vortices was impressed on the aft face of the forward-radome, balancing the pressure-drag on the front face and producing very little net pressure-drag.

An uncertainty analysis of the measured aerodynamic coefficients was performed. The uncertainties considered stemmed from pressure transducer calibrations and

Pitot/static probe assembly traverse distances.

All measured aerodynamic coefficients were corrected for compressibility effects using the first Prandtl-Glauert rule and tabulated in Section 4.5 in Tables 4.5 to 4.8. All coefficients are assumed to apply to the full-scale aircraft.

5.2 Recommendations

Some recommendations for future related studies include:

- Tests with the radomes in yawed configuration for all radome separation distances. Also, static taps should be inserted on *both* sides of the radomes, for side force determination.
- Tests at various Reynolds numbers to ensure repeatability of measured aerodynamic coefficients
- Longer guide rods for the horizontal traverse of the wake traverse gear. This would permit measurements in *one* plane rather than two adjacent planes, saving running time and data reduction time.
- Use of a standard Pitot probe (having two concentric tubes with total and static pressure interfaces) rather than a Pitot/static probe assembly with two tubes separated by 0.75". This would permit choice of the probe's horizontal step-size, instead of the fixed horizontal step-size of 0.75" used in the experiment so that total and static pressures measurements would coincide spatially. This would also simplify the data reduction program.

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Appendix A: Sample PMARC Input File

```

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&BINP5 NTSTPS=1, DTSTEP=2.0, &END
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&BINP8 ALDEG=0, YAWDEG=0.0, THEDOT=0.0, PSIDOT=0.0,
    PHIDOT=0.0, &END
&BINP9 CBAR=23.226, SREF=112.386, SSPAN=7.5, &END
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    NOCF=0, NOCL=0, VNORM=0.0, &END
&BINP12 KPAN=0, KSIDE=0, NEWNAB=0, NEWSID=0, &END

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    0.0 0.0 0.0
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    6.82650 1.80331 0.0
    8.20400 2.71816 0.0
    9.65400 3.21511 0.0
    10.7415 3.41065 0.0

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13.7865  3.20968  0.125
15.22451 2.78638  0.2407007
16.63123 2.15517  0.4795534
17.43126 1.53534  0.5991155
18.17358 0.77705  0.7051442
18.34262 0.0      0.728115
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6.82650  15.0  0.0
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9.65400  15.0  0.0
10.7415  15.0  0.0
11.6840  15.0  0.0
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13.7865  15.0  0.125
15.22451 15.0  0.2407007
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  0.81307 0.68324 0.0
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    3.60408  1.32791  0.0
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      42.12814 15.0 0.51939
      43.44990 15.0 0.44033
      45.40100 15.0 0.32475
      47.50544 15.0 0.15010
      49.81900 15.0 0.00000
      51.26900 15.0 0.00000
      52.71900 15.0 0.00000
      53.08150 15.0 0.00000
&BPNODE TNODE=3, TNPC=0, TINTC=0,

&PATCH1 IREV=0, IDPAT=2, MAKE=0, KCOMP=1, KASS=1,
AFTFLOOR
&SECT1 STX=53.08150, STY=0.0, STZ=0.0, SCALE=1.0,
      ALF=0.0, THETA=0.0,
      INMODE=1, TNODS=0, TNPS=0, TINTS=0,
      15.0 0.0 0.0
      0.0 0.0 0.0
&BPNODE TNODE=3, TNPC=5, TINTC=0,
&SECT1 STX=72.0, STY=0.0, STZ=0.0, SCALE=1.0,
      ALF=0.0, THETA=0.0,
      INMODE=0, TNODS=5, TNPS=5, TINTS=0,

&WAKE1 IDWAK=0, IFLXW=0,

&VS1 NVOLR=0, NVOLC=0,

&SLIN1 NSTLIN=0,

```

Appendix B: Radome Surface Geometry

Note:

1. All coordinates are relative to "baseplane",
as defined in Section 3.1.1
2. X, Y, Z coordinates: axial; span; vertical
3. Axial coordinates are relative to leading edge of radome

Forward-Radome Surface Geometry

Model			Full-Scale		
X(in)	Y(in)	Z(in)	X(in)	Y(in)	Z(in)
0.000	0.000	0.000	0.0	0.000	0.000
0.392	1.328	0.000	2.7	9.158	0.000
	1.125	0.407		7.756	2.805
	0.813	0.683		5.607	4.712
	0.461	0.795		3.177	5.485
	0.152	0.824		1.047	5.681
	0.000	0.827		0.000	5.700
0.827	1.803	0.000	5.7	12.437	0.000
	1.626	0.585		11.215	4.034
	1.258	1.050		8.675	7.245
	0.757	1.301		5.222	8.970
	0.245	1.363		1.691	9.400
	0.000	1.368		0.000	9.432
2.204	2.718	0.000	15.2	18.746	0.000
	2.542	0.926		17.529	6.387
	2.078	1.755		14.331	12.103
	1.317	2.271		9.080	15.660
	0.423	2.426		2.915	16.728
	0.000	2.439		0.000	16.818
3.654	3.215	0.000	25.2	22.173	0.000
	3.026	1.109		20.869	7.645
	2.542	2.126		17.532	14.666
	1.625	2.810		11.205	19.381
	0.528	3.021		3.642	20.832
	0.000	3.035		0.000	20.931

Model			Full-Scale		
X(in)	Y(in)	Z(in)	X(in)	Y(in)	Z(in)
4.742	3.411	0.000	32.7	23.522	0.000
	3.224	1.159		22.236	7.991
	2.697	2.247		18.599	15.497
	1.724	2.998		11.893	20.679
	0.560	3.227		3.861	22.254
	0.000	3.239		0.000	22.338
5.684	3.441	0.000	39.2	23.729	0.000
	3.255	1.173		22.446	8.090
	2.735	2.269		18.862	15.647
	1.748	3.028		12.054	20.884
	0.568	3.257		3.917	22.460
	0.000	3.277		0.000	22.598
6.699	3.380	0.000	46.2	23.313	0.000
	3.184	1.164		21.960	8.029
	2.662	2.240		18.361	15.451
	1.713	2.976		11.817	20.521
	0.554	3.201		3.823	22.078
	0.000	3.216		0.000	22.181
7.787	3.222	0.000	53.7	22.221	0.000
	3.025	1.098		20.861	7.575
	2.525	2.114		17.413	14.580
	1.606	2.796		11.073	19.284
	0.522	3.006		3.598	20.731
	0.000	3.020		0.000	20.831
9.237	2.809	0.000	63.7	19.369	0.000
	2.628	0.960		18.123	6.623
	2.174	1.813		14.990	12.502
	1.374	2.373		9.473	16.363
	0.452	2.545		3.114	17.549
	0.000	2.555		0.000	17.623
10.687	2.206	0.000	73.7	15.215	0.000
	2.035	0.741		14.037	5.112
	1.628	1.360		11.230	9.377
	1.005	1.727		6.928	11.914
	0.329	1.834		2.272	12.645
	0.000	1.841		0.000	12.698

Model			Full-Scale		
X(in)	Y(in)	Z(in)	X(in)	Y(in)	Z(in)
11.480	1.713	0.000	79.2	11.812	0.000
	1.520	0.554		10.483	3.818
	1.150	0.975		7.933	6.723
	0.685	1.173		4.723	8.088
	0.216	1.228		1.491	8.468
	0.000	1.233		0.000	8.500
12.209	1.319	0.000	84.2	9.094	0.000
	1.115	0.412		7.692	2.842
	0.795	0.674		5.484	4.651
	0.456	0.779		3.148	5.374
	0.146	0.808		1.010	5.576
	0.000	0.811		0.000	5.597
12.553	1.092	0.000	86.6	7.530	0.000
	0.878	0.325		6.057	2.242
	0.595	0.495		4.102	3.411
	0.317	0.559		2.187	3.854
	0.099	0.577		0.683	3.980
	0.000	0.579		0.000	3.991

Aft-Radome Surface Geometry

Model			Full-Scale		
X(in)	Y(in)	Z(in)	X(in)	Y(in)	Z(in)
0.000	1.035	0.000	0.0	7.139	0.000
	0.718	0.270		4.950	1.860
	0.444	0.376		3.059	2.597
	0.230	0.419		1.583	2.894
	0.076	0.430		0.526	2.966
	0.000	0.431		0.000	2.975
0.653	1.779	0.000	4.5	12.270	0.000
	1.519	0.570		10.479	3.929
	1.060	0.931		7.311	6.424
	0.616	1.089		4.245	7.512
	0.194	1.153		1.337	7.950
	0.000	1.161		0.000	8.007

Model			Full-Scale		
X(in)	Y(in)	Z(in)	X(in)	Y(in)	Z(in)
1.958	2.630	0.000	13.5	18.140	0.000
	2.394	0.890		16.512	6.135
	1.813	1.565		12.502	10.796
	1.070	1.935		7.376	13.349
	0.349	2.078		2.407	14.332
	0.000	2.095		0.000	14.445
3.915	3.558	0.000	27.0	24.540	0.000
	3.208	1.202		22.122	8.288
	2.512	2.168		17.322	14.952
	1.500	2.703		10.348	18.645
	0.489	2.902		3.370	20.011
	0.000	2.934		0.000	20.234
6.018	3.972	0.000	41.5	27.395	0.000
	3.604	1.328		24.856	9.158
	2.844	2.438		19.610	16.818
	1.696	2.987		11.699	20.598
	0.547	3.182		3.773	21.943
	0.000	3.208		0.000	22.128
8.338	3.637	0.000	57.5	25.081	0.000
	3.249	1.221		22.405	8.421
	2.498	2.152		17.230	14.839
	1.483	2.622		10.226	18.081
	0.486	2.769		3.351	19.099
	0.000	2.784		0.000	19.199
9.788	2.859	0.000	67.5	19.718	0.000
	2.413	0.900		16.639	6.210
	1.736	1.491		11.975	10.286
	0.990	1.753		6.828	12.092
	0.312	1.850		2.151	12.760
	0.000	1.867		0.000	12.876
11.238	1.334	0.000	77.5	9.202	0.000
	0.756	0.279		5.213	1.927
	0.421	0.359		2.901	2.477
	0.223	0.391		1.535	2.699
	0.070	0.400		0.484	2.757
	0.000	0.400		0.000	2.759
11.600	0.000	0.000	80.0	0.000	0.000

Forward-Radome Static Tap Coordinates

Model			Full-Scale		
X(in)	Y(in)	Z(in)	X(in)	Y(in)	Z(in)
0.392	1.236	0.231	2.7	8.523	1.592
	0.830	0.674		5.724	4.646
	0.249	0.819		1.719	5.648
	0.000	0.827		0.000	5.700
0.827	1.722	0.360	5.7	11.872	2.483
	1.212	1.086		8.357	7.489
	0.370	1.357		2.553	9.361
	0.000	1.368		0.000	9.432
2.204	2.634	0.597	15.2	18.163	4.120
	1.951	1.883		13.457	12.983
	0.608	2.413		4.190	16.644
	0.000	2.439		0.000	16.818
3.654	3.116	0.726	25.2	21.488	5.003
	2.361	2.327		16.284	16.051
	0.739	3.006		5.099	20.728
	0.000	3.035		0.000	20.931
4.742	3.316	0.775	32.7	22.868	5.346
	2.499	2.471		17.233	17.043
	0.787	3.211		5.424	22.144
	0.000	3.239		0.000	22.338
5.684	3.345	0.783	39.2	23.071	5.399
	2.530	2.502		17.446	17.252
	0.795	3.240		5.479	22.346
	0.000	3.277		0.000	22.598
6.699	3.280	0.767	46.2	22.621	5.290
	2.476	2.452		17.076	16.913
	0.780	3.186		5.377	21.970
	0.000	3.216		0.000	22.181
7.787	3.118	0.722	53.7	21.504	4.982
	2.348	2.309		16.192	15.922
	0.736	2.990		5.077	20.621
	0.000	3.020		0.000	20.830
9.237	2.719	0.621	63.7	18.752	4.280
	2.026	1.966		13.972	13.557
	0.632	2.532		4.358	17.461
	0.000	2.555		0.000	17.622

Model			Full-Scale		
X(in)	Y(in)	Z(in)	X(in)	Y(in)	Z(in)
10.687	2.128	0.466	73.7	14.679	3.214
	1.540	1.438		10.617	9.918
	0.475	1.825		3.273	12.583
	0.000	1.841		0.000	12.697
11.484	1.625	0.330	79.2	11.209	2.276
	1.131	0.988		7.799	6.815
	0.343	1.221		2.363	8.420
	0.000	1.233		0.000	8.500
12.209	1.233	0.233	84.2	8.503	1.606
	0.822	0.660		5.670	4.552
	0.247	0.803		1.701	5.539
	0.000	0.811		0.000	5.596

Aft-Radome Static Tap Coordinates

Model			Full-Scale		
X(in)	Y(in)	Z(in)	X(in)	Y(in)	Z(in)
0.653	1.677	0.328	4.5	11.567	2.265
	1.113	0.903		7.673	6.229
	0.337	1.139		2.321	7.852
	0.000	1.161		0.000	8.007
1.958	2.542	0.544	13.5	17.534	3.749
	1.771	1.596		12.216	11.004
	0.544	2.055		3.752	14.172
	0.000	2.095		0.000	14.445
3.915	3.393	0.724	27.0	23.401	4.991
	2.426	2.238		16.730	15.435
	0.745	2.871		5.139	19.800
	0.000	2.934		0.000	20.234
6.018	3.791	0.804	41.5	26.145	5.544
	2.742	2.518		18.909	17.365
	0.833	3.153		5.748	21.746
	0.000	3.208		0.000	22.127
8.338	3.464	0.720	57.5	23.887	4.967
	2.444	2.192		16.852	15.117
	0.745	2.749		5.134	18.961
	0.000	2.784		0.000	19.198

Model			Full-Scale		
X(in)	Y(in)	Z(in)	X(in)	Y(in)	Z(in)
9.788	2.665	0.505	67.5	18.382	3.485
	1.786	1.463		12.319	10.089
	0.539	1.828		3.717	12.605
	0.000	1.867		0.000	12.876
11.238	1.158	0.114	77.5	7.985	0.783
	0.696	0.297		4.797	2.045
	0.206	0.393		1.423	2.710
	0.000	0.400		0.000	2.759

Fairing Surface Geometry

Note: coordinates are relative to leading edge of forward-radome

Model		Full-Scale	
X(in)	Z(in)	X(in)	Z(in)
12.343	0.728	85.1	5.021
12.907	0.802	89.0	5.528
14.550	0.972	100.3	6.702
17.120	1.083	118.1	7.472
20.361	1.067	140.4	7.361
23.954	1.054	165.2	7.270
27.547	1.002	190.0	6.910
30.785	0.856	212.3	5.903
33.352	0.695	230.0	4.794
35.001	0.589	241.4	4.062
35.569	0.554	245.3	3.819

Appendix C: DRAG Program

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C PROGRAM DRAG
C APRIL, 1998
C THIS PROGRAM CALCULATES THE TOTAL DRAG OF THE RADOMES IN THE WIND-TUNNEL

C*****
C VARIABLE DEFINITIONS

C PTOTC2(I,J): TOTAL PRESSURE - REFERENCE PRESSURE;
C RECORDED BY SETRA PRESSURE TRANSDUCER 1 (Pa)
C PTMXC2: FREESTREAM TOTAL PRESSURE - REFERENCE PRESSURE
C PTC2(I,J): AVERAGE OF FOUR PTOTC2 VALUES SURROUNDING A GRID PANEL
C PSTAC2(I,J): STATIC PRESSURE - REFERENCE PRESSURE;
C RECORDED BY SETRA PRESSURE TRANSDUCER 1(Pa)
C PINFC2 FREESTREAM STATIC PRESSURE - REFERENCE PRESSURE
C PSC2(I,J): AVERAGE OF FOUR PSTAC2 VALUES SURROUNDING A GRID PANEL
C X: FREESTREAM DYNAMIC PRESSURE
C PC12(I,J): UPSTREAM CONTRACTION PRESSURE - DOWNSTREAM
C CONTRACTION PRESSURE; RECORDED BY SETRA
C PRESSURE TRANSDUCER 2 (Pa)
C PD12(I,J): AVERAGE OF FOUR PC12 VALUES SURROUNDING A GRID PANEL
C PSDYN(I,J): PSC2(I,J)/PD12(I,J); STATIC PRESSURE NON-DIMENSIONALIZED
C PTDYN(I,J): PTC2(I,J)/PD12(I,J); TOTAL PRESSURE NON-DIMENSIONALIZED
C PAMB: AMBIENT PRESSURE (mmHg)
C TAMB: AMBIENT TEMPERATURE (deg F)
C RHO: AMBIENT AIR DENSITY (kg/m3)
C NPANS: NUMBER OF PANELS IN MEASUREMENT PLANE
C NCOLX: NUMBER OF VERTICAL COLUMNS TRAVERSED IN MEASUREMENT PLANE
C NCOL: NCOLX-2 (FIRST PTOTC2 AND LAST PSTAC2 VALUES IGNORED
C TO ACCOUNT FOR SEPARATION DISTANCE BETWEEN TOTAL & STATIC
C PROBES)
C NROW: NUMBER OF HORIZONTAL ROWS TRAVERSED IN MEASUREMENT PLANE
C NGRID: TOTAL NUMBER OF GRID POINTS (NCOL*NROW)
C APAN: AREA OF EACH PANEL
C HORIZ(I,J): HORIZONTAL GRID POINT POSITION (m)
C VERT(I,J): VERTICAL GRID POINT POSITION (m)
C HORCEN(I,J): HORIZONTAL CENTROID POSITION BETWEEN TWO ADJACENT
C HORIZONTAL GRID POINTS (m)
C VERCEN(I,J): VERTICAL CENTROID POSITION BETWEEN TWO ADJACENT
C VERTICAL GRID POINTS (m)
C VIN(I,J): VELOCITY AT TEST-SECTION INLET (m/s)
C VOUT(I,J): VELOCITY AT PANEL CENTROID IN MEASUREMENT PLANE (m/s)
C G,P: VARIABLES USED IN BETZ'S FORMULA FOR DRAG
C DPAN: DRAG FORCE ACTING ON PANEL IN MEASUREMENT PLANE (N)
C DPANAR: DPAN/APAN (N/m2)
C DTOT: SUM OF DRAG VALUES OF PANELS IN SELECTED SURVEY (N)
C FDTOT: SUM OF ALL DTOT (TOTAL DRAG FROM ALL SURVEYS) (N)
C CDPAN: DRAG COEFFICIENT OF PANEL IN MEASUREMENT PLANE
C CDAR: CDPAN/APAN (1/m2); MOMENTUM-FLUX DEFICIT COEFFICIENT
C CDTOT: SUM OF DRAG COEFFICIENTS OF PANELS IN SELECTED SURVEY
C FCDTOT: SUM OF ALL CDTOT (TOTAL DRAG COEFFICIENT FROM ALL SURVEYS)

C UNCERTAINTY ANALYSIS VARIABLES
C -----
C PARTIAL DERIVATIVES FROM BETZ'S FORMULA:
C WPT: DERIVATIVE OF CD W.R.T. (PTC2-PINFC2)
C WPS: DERIVATIVE OF CD W.R.T. (PSC2-PINFC2)
C WPTX: DERIVATIVE OF CD W.R.T. (PTMXC2-PINFC2)
C WA: DERIVATIVE OF CD W.R.T. (APAN)
C DELTR1: UNCERTAINTY IN SETRA TRANSDUCER 1
C DELCD: UNCERTAINTY IN CD FOR EACH PANEL
C DELCDT: TOTAL UNCERTAINTY IN CD (SUMMED OVER ALL PANELS)
C DELAR: UNCERTAINTY IN PANEL AREA
C*****

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      REAL PTOTC2(50,50), PTC2(50,50), PSTAC2(50,50), PSC2(50,50),
+      PC12(50,50), PD12(50,50), RHO, TAMB, PAMB, APAN(50,50),
+      VOUT(50,50), VIN(50,50), DPAN(50,50), CDPAN(50,50),
+      G, P, CDTOT, DTOT, FCDTOT, FDTOT, HORIZ(50,50),
+      VERT(50,50), HRCEN(50,50), VERCEN(50,50), PSDYN(50,50),
+      PTDYN(50,50), DPANAR(50,50), CDANAR(50,50), VOUTIN(50,50),
+      PINFC2, PTMXC2, CPPAN(50,50), SUM1, SUM2, WPT, WPS,
+      WPTX, WA, DELTR1, DELCD, DELCDT, X, Y, Z
      INTEGER NPANSX, NPANS, NCOLX, NCOL, NROW, NGRIDX, NGRID,
+      DJ, NUM
      CHARACTER*1 ANS

C      OPEN INPUT AND OUTPUT FILES

      OPEN (1, FILE='r1bld.for')
      OPEN (2, FILE='r1bld.out')
C      NOTE: r=RAD#, b=BLOCK#, d=DESIGN SEP., f=1.1XSEP., c=0.8XSEP.

C      INITIALIZING VARIABLES

      FDTOT = 0
      FCDTOT = 0
      DELTR1 = 19.0

4      PRINT*, 'PLEASE ENTER THE AMBIENT PRESSURE (mmHg): '
      READ*, PAMB
      PRINT*, 'PLEASE ENTER THE AMBIENT TEMPERATURE (deg F): '
      READ*, TAMB
      PRINT*, 'PLEASE ENTER NUMBER OF MEASUREMENTS IN HORIZ. TRAVERSE'
      READ*, NCOLX
      PRINT*, 'PLEASE ENTER NUMBER OF MEASUREMENTS IN VERT. TRAVERSE'
      READ*, NROW
      RHO = PAMB*133.3223/(287*((TAMB-32)*5/9 + 273.15))
      NCOL=NCOLX-1
      NPANSX = (NCOLX-1)*(NROW-1)
      NPANS = (NCOL-1)*(NROW-1)
      NGRIDX = NCOLX*NROW
      NGRID = NCOL*NROW

C      ORGANIZE WIND-TUNNEL DATA FROM INPUT FILE INTO AN I x J GRID

      DJ = 1
      I = 1
      J = 1
      NUM=1
      DO 12 K = 1, NGRIDX
C      PRINT*, NUM
      NUM=NUM+1
      READ(1,*) HORIZ(I,J), VERT(I,J), PTOTC2(I,J), PSTAC2(I,J),
+      PC12(I,J)
      WRITE(3,16) HORIZ(I,J), VERT(I,J), PTOTC2(I,J),
+      PSTAC2(I,J), PC12(I,J)
16      FORMAT (F6.4, 2X, F6.4, 2X, F8.3, 2X, F8.3, 8X, F8.3)
      J = J + DJ
      IF (J.GT.NCOLX) THEN
          J = NCOLX
          DJ = -1
          I = I + 1
      ENDIF
C      IF (J.LT.1) THEN
          J = 1
          DJ = 1
          I = I + 1
      ENDIF
12      CONTINUE

```

```

C      COINCIDE VARIABLES SPATIALLY TO ACCOUNT FOR THE 0.75 IN. SEPARATION
C      DISTANCE BETWEEN THE STATIC AND TOTAL PRESSURE TUBES

      DO 24 I = 1, NROW
        DO 23 J = 1, NCOL
          PTOTC2(I,J) = PTOTC2(I,J+1)
          HORIZ(I,J) = HORIZ(I,J+1)
23      CONTINUE
24      CONTINUE

C      COMPUTE FREESTREAM TOTAL AND STATIC PRESSURES IN MEASUREMENT PLANE

      SUM1 = 0.0
      SUM2 = 0.0
      DO 40 J = 2, NCOL
        SUM1 = SUM1 + PSTAC2(NROW,J)
        SUM2 = SUM2 + PTOTC2(NROW,J)
40      CONTINUE
      PINFC2 = SUM1 / (NCOL-1)
      PTMXC2 = SUM2 / (NCOL-1)
      X = PTMXC2 - PINFC2

C      COMPUTE TOTAL DRAG AND PERFORM UNCERTAINTY ANALYSIS

      WRITE (2,*) ' HOR      VER      PT/DY      PS/DY      PD12      VOUT/V
+IN DPAN/AR      CDPAN/AR      CPPAN      AR'
      WRITE (2,*) ' (m)      (m)      ( )      ( )      (Pa)
+ (N/m2)      (1/m2)'
      WRITE (2,*)
      CDTOT = 0
      DTOT = 0
      DELCDT = 0
      DO 20 I = 1, NROW-1
        DO 25 J = 1, NCOL-2
          PTC2(I,J) = (PTOTC2(I,J) - PTOTC2(I,J+1) - PTOTC2(I+1,J)
+                    + PTOTC2(I+1,J+1)) / 4
          PSC2(I,J) = (PSTAC2(I,J) + PSTAC2(I,J+1) - PSTAC2(I+1,J)
+                    - PSTAC2(I+1,J+1)) / 4
          PD12(I,J) = (PC12(I,J) - PC12(I,J+1) + PC12(I+1,J)
+                    - PC12(I+1,J+1)) / 4
          Y = PTC2(I,J) - PINFC2
          Z = PSC2(I,J) - PINFC2
          PSDYN(I,J) = PSC2(I,J) / X
          PTDYN(I,J) = PTC2(I,J) / X
          APAN(I,J) = ABS((HORIZ(I,J+1)-HORIZ(I,J)))*(VERT(I+1,J)-
+                    VERT(I,J)))
          HORCEN(I,J) = (HORIZ(I,J+1) - HORIZ(I,J))/2
          VERCEN(I,J) = (VERT(I+1,J) + VERT(I,J))/2
          VOUT(I,J) = (2/RHO*(PTC2(I,J)-PSTAC2(I,J))**0.5
          VIN(I,J) = (2/RHO*X)**0.5
          VOUTIN(I,J) = VOUT(I,J) / VIN(I,J)

C      BETZ'S FORMULA FOR DRAG

      G = Y / X
      P = Z / X
      CPPAN(I,J) = P
      DPAN(I,J) = X*(1-G-((1-P)**0.5-
+                    (G-P)**0.5)*(2-(1-P)**0.5-(G-P)**0.5))*
+                    APAN(I,J)

C      UNCERTAINTY IN CD

      WPT = (1/0.0725069)*(-1/X - (-0.5*((Y-Z)/X)**(-0.5)
+                    *1/X)*(-0.5*((Y-Z)/X)**(-0.5)*1/X))*APAN(I,J)
      WPS = (1/0.0725069)*(-0.5*(1-Z/X)**(-0.5)*(-1/X) - 0.5*
+                    ((Y-Z)/X)**(-0.5)*(-1/X))*(-0.5*(1-Z/X)**(-0.5)*

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+          (-1/X) - 0.5*((Y-Z)/X)**(-0.5)*(-1/X))*APAN(I,J)
WPTX = (1/0.0725069)*(Y/X**2 - (0.5*(1-Z/X)**(-0.5)*
+          Z/X**2 - 0.5*((Y-Z)/X)**(-0.5)*(-Y/X**2+Z/X**2))
+          * (-0.5*(1-Z/X)**(-0.5)*(Z/X**2) - 0.5*((Y-Z)/X)
+          **(-0.5)*(-Y/X**2+Z/X**2))*APAN(I,J)
WA = DPAN(I,J)/(X*0.0725069*APAN(I,J))
DELAR = (((HORIZ(I,J+1)-HORIZ(I,J))*0.00025)**2 -
+          ((VERT(I+1,J)-VERT(I,J))*0.00025)**2)**0.5
DELCD = ((WPT*DELTRL)**2 + (WPS*DELTRL)**2 + (WPTX
+          *DELTRL)**2 + (WA*DELAR)**2)**0.5
DELCDT = DELCDT + DELCD

IF (DPAN(I,J).LT.0) DPAN(I,J) = 0.0
CDPAN(I,J) = DPAN(I,J) / ((PTMXC2-PINFC2)*0.072506952)
DPANAR(I,J) = DPAN(I,J) / APAN(I,J)
CDANAR(I,J) = CDPAN(I,J) / APAN(I,J)
IF (I.GT.2) THEN
CDTOT = CDTOT + CDPAN(I,J)
PRINT*,CDTOT, DELCDT
DTOT = DTOT + DPAN(I,J)

C      WRITE COMPUTED VARIABLES TO OUTPUT FILE

      WRITE (2,30) HORCEN(I,J), VERCEN(I,J), PTDYN(I,J),
+          PSDYN(I,J), PD12(I,J), VOUTIN(I,J),
+          DPANAR(I,J), CDANAR(I,J), CPPAN(I,J),
+          APAN(I,J)
30      FORMAT (F6.4,2X,F6.4,2X,F8.5,2X,F8.5,1X,F8.3,1X,F7.5,
+          1X,F9.3,1X,F10.7,1X,F10.5,1X,F12.11)
      ENDIF
25      CONTINUE
20      CONTINUE
      WRITE (2,*)
      WRITE (2,35) DTOT, CDTOT
35      FORMAT ('DRAG: ',F7.3, ' ', DRAG COEFFICIENT: ',F10.7)
      PRINT*, 'ARE THERE ANY MORE HORIZONTAL TRAVERSES (Y/N)?'
50      READ*, ANS
      IF (ANS.EQ.'Y') THEN
          FDTOT = FDTOT + DTOT
          FCDTOT = FCDTOT + CDTOT
          DO 53 I = 1, NCOLX
              BACKSPACE (UNIT=1)
53          CONTINUE
              GOTO 4
          ELSEIF (ANS.EQ.'N') THEN
              FDTOT = FDTOT + DTOT
              FCDTOT = FCDTOT + CDTOT
              GOTO 100
          ELSE
              PRINT*, 'PLEASE ENTER Y OR N'
              GOTO 50
          ENDIF
100     WRITE (2,*)
          WRITE(2,55) FDTOT, FCDTOT
55     FORMAT ('TOTAL DRAG: ',F7.3, ' TOTAL DRAG COEFFICIENT: ',F10.7)
      PRINT*, PTMXC2, PINFC2
      END

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IMAGE EVALUATION TEST TARGET (QA-3)

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